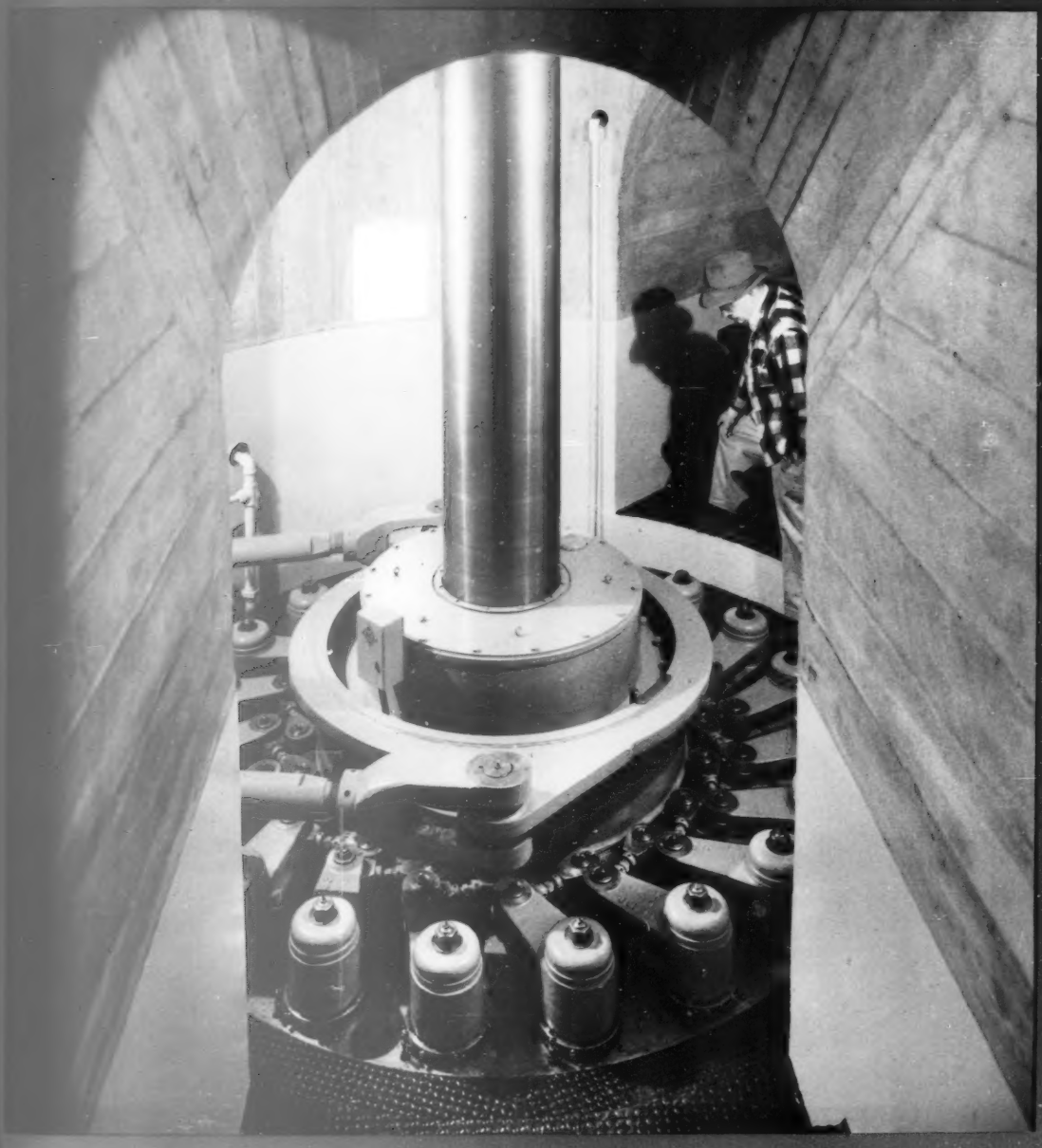
ALLIS-CHALMERS

Electrical REVIEW

2ND QUARTER 1953



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See page 30 for full description

ALLIS-CHALMERS Electrical REVIEW

THE COVER

IN THE TURBINE PIT — which is below a 16,000-kva, 300-rpm suspended-type generator and above a 17,000-hp, 293-ft head Francis waterwheel—this governor controlled gate mechanism matches turbine output to generator load requirements. As the governor regulating shaft moves, the wicket gate operating ring, surrounding the bearing housing and turbine shaft, transmits motion through the links and gate levers to wicket gates in the hydraulic turbine. The wicket gates in turn control the flow of water from a West Coast river into the Francis runner to maintain constant speed on the 85 percent pf, 11,500-volt direct connected generator regardless of load.

A-C Staff Photo by M. Durante

Allis-Chalmers

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PART 1

HEAVY MOTOR TRANSPORT came later than this steam turbine.

Steam Turbine HISTORY HIGHLIGHTS

YOUR AUTHOR is an outstanding turbine engineer of long experience. His summary of the historical milestones in thermal turbine history is tempered with his 40 years of study and a personal acquaintance with the men who pioneered in turbine developments and applications.

R. C. Allen has been intimately concerned with the design and application of turbines as chief engineer and later as Steam Turbine Department manager. He is now consulting engineer for Allis-Chalmers General Machinery Division.

by R. C. ALLEN

The Progress in steam turbine development is told in three parts, starting with the first practical application.

WHILE most historical reviews of steam turbine developments start with a mention of the turbine built by Hero of Alexandria in about 120 B.C., the practical phase of the story starts with the issuance of a United States patent in 1831 to Foster and Avery for a reaction wheel of the Hero type. This appears to be the first steam turbine invention which was developed and actually applied to do commercial work. Dr. Robert H. Thurston, in a paper before the ASME in 1900, stated that several of these machines were in operation in 1835, some of which were used to drive saw mills near Syracuse, New York. Information which Dr. Thurston secured from Prof. John E. Sweet includes a dimensioned sketch of an Avery wheel which is said to have been reproduced from Avery's original notes. A copy of this sketch is shown in Figure 1. The sketch shows two hollow rotating arms 2½ feet long, carried on a hollow shaft through which the steam was admitted. The tangential openings at the ends of the arms for the steam jets were said to have been ⅛ inch by ¼ inch.

Some fifty of these units were built for saw mills and woodworking shops. They were belt-connected to the driven machinery.

One of the Avery reaction turbines with 3½-ft arms was placed on a locomotive in 1836 and operated on a railroad near Newark, New Jersey. According to these records, the tip speed of the rotating arms was 14¼ miles per minute.

The Avery turbines were not free from troubles. The records indicate that these turbines had to be taken to a blacksmith shop every three or four months for repairs and that this low order of reliability had much to do with the abandonment of this prime mover. While the turbines were very noisy, the efficiency was said to be about the same as that of corresponding reciprocating engines.

A brief review of the work of subsequent turbine development shows that DeLaval and others directed some effort toward the application of the Hero type of reaction turbine. Figure 2 shows the simple reaction turbine built by DeLaval in 1883. Sir Charles Parsons built a counterbalanced single-arm Hero wheel in 1893. The drawing in Figure 3 shows this arrangement. He followed this by building a three-stage double-arm Hero turbine. However, due largely to steam leakage and to the high frictional resistance of the rotating arms, these developments did not produce results of sufficient commercial interest to warrant a continuance of this type of design.

An early English patent by the Harthan brothers in 1858 shows a well-defined conception of a velocity-compounded wheel such as was later developed by Curtis. The drawing included in the Harthan patent did not indicate a working knowledge of the characteristics of the flow of steam through turbine blade passages. A copy of the patent drawing is shown in Figure 4.

To one interested in the engineering of elastic fluid turbines, there is a never-ending source of interest in a review of the multitude of types of steam turbines proposed and, in many cases, built and tested during the last

half of the nineteenth century. It appears, however, that not until the 1880's was logical engineering analysis applied to the design and proportioning of the fluid flow components of steam turbines.

Reaction turbine developed

Sir Charles Parsons' earliest work around 1884 was directed toward efficient steam turbine designs of relatively low cost. His was the first sound engineering attack on steam turbine design. His designs employed low blade speeds, although some of his earliest machines of low output operated at speeds of rotation between 10,000 and 20,000 rpm. The first Parsons turbine is shown in Figure 5.

Parsons was particularly diligent in developing turbine blade constructions so that his many-stage turbines could be equipped with nonferrous blades which could easily be formed by means of the draw bench. Simple and effective blade fastenings have always been a characteristic feature of these reaction turbines. Figures 6 and 7 show pioneer turbines of this type installed in the United States near the beginning of this century.

Parsons early recognized the dynamic relations that determine efficiency and require an optimum relation between blade speed and steam speed. He also adopted the principle of approximately equal heat drops in moving and stationary blades of each stage. One of his earliest contributions to engineering calculations was the use of the "equivalent head" principle, based on the assumption that the pressure in pounds per square foot could be represented, in practical problems, by the height of a homogeneous head of steam at the density corresponding to the initial pressure and temperature. This procedure in its elementary form was adequate when pressures were low. It is still a useful method for present-day high pressure work, provided the necessary corrections are introduced to compensate for the theoretical deviations from the elementary rule. Further corrections are now used for the differences in the properties of steam from the properties of a perfect gas.

Single-stage impulse turbines applied

Dr. Carl Gustav Patrik DeLaval started his work on turbine development prior to 1889. His efforts were mainly directed toward the design and building of well-engineered single-stage impulse turbines. The drawing in Figure 8 shows one of these units. Some of the outstanding principles applied by this pioneer follow:

(a) Serious attempts at cost reduction by the employment of small-diameter single-stage wheels operated at high rotative speeds.

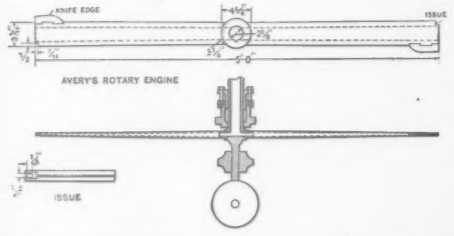
(b) DeLaval adopted the principle of operating a rotating shaft above its first critical speed and recognized the advantages to smooth running in so doing. He employed bearings with angular flexibility and with controlled damping forces.

(c) He worked out design patterns for the prediction of stresses in impulse blades and disks at high peripheral speeds. Some of the early DeLaval turbines operated with a blade tip speed of about 1450 ft per second.

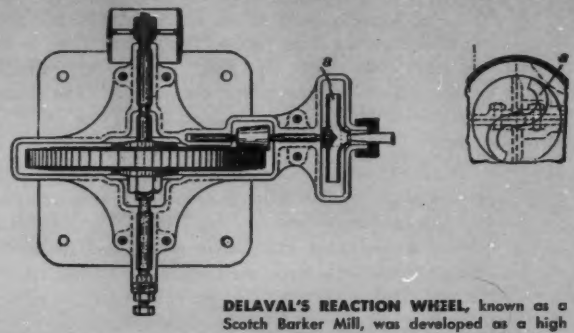
(d) DeLaval applied helical reduction gears to his earliest machines which were used for pumps and generators.

Multi-stage impulse turbine development started

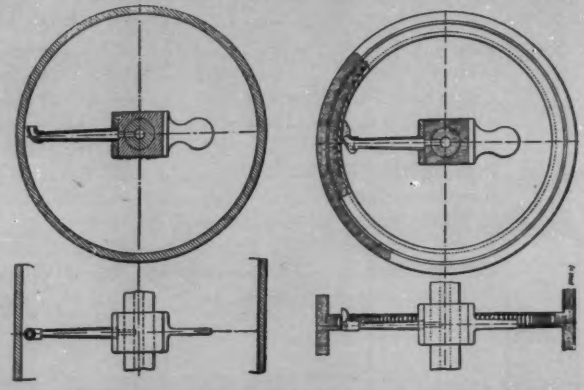
Prof. A. C. E. Rateau and his associated firm of Sautter-Harle in France started the development of multi-stage impulse turbines around 1894. He recognized the possi-



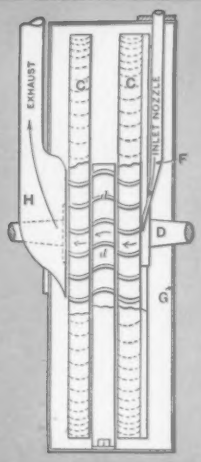
HIGH PERIPHERAL SPEEDS and stuffing boxes caused trouble with Avery's reaction wheel. Knife edges required frequent replacement and speed reduction was a serious problem. **(FIGURE 1)**



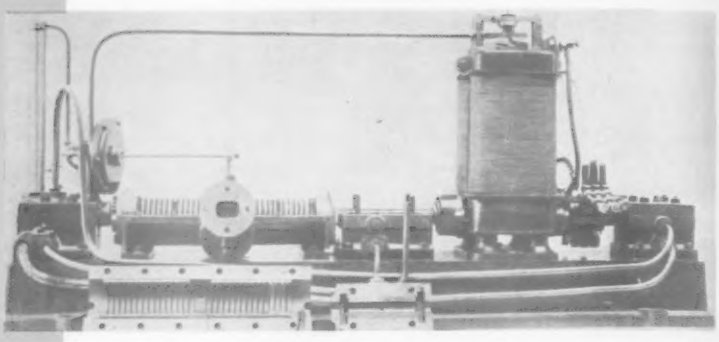
DELAVAL'S REACTION WHEEL, known as a Scotch Barker Mill, was developed as a high speed cream separator drive. **(FIGURE 2)**



COUNTERBALANCED HERO WHEEL (left) was tried by Parsons and later modified by the addition of a bladed ring which redirected the steam into a moving bucket (right). **(FIGURE 3)**



INLET STEAM passing through the first wheel of the Harthan turbine is diverted into the second wheel by stationary blades. **(FIGURE 4)**



PARSONS' FIRST TURBINE was of the parallel-flow type, which was found to be the most efficient arrangement. Steam entered at center and flowed to both ends. **(FIGURE 5)**

bility of reducing the number of stages in a turbine by increasing the heat drops over the stationary blades and having substantially no pressure drops over the moving blades. An early Rateau wheel is shown in Figure 9. Prof. Rateau's broad knowledge of mathematics and engineering enabled him to apply sound physical principles to the proportioning of the fluid flow passages and to the structural mechanics of his machines.

The early Rateau turbines were equipped with horizontally split diaphragms which contained the stationary blades or nozzles. Some of the first machines used rotating blade-carrying disks formed from steel plates pressed into large angle cones, two such plates being assembled by riveting either side of the central hub. The pressed steel moving blades were assembled by riveting the blades to the rims of each disk assembly, as shown in Figure 10.

The late Charles G. Curtis started his work in the development of the steam turbine prior to 1897. One important group of his patents was issued in 1896. Curtis proposed velocity-compounded turbines in single and multi-stage arrangements. His work indicated a good knowledge of the engineering necessary to the calculation of efficient fluid flow passages. He contributed many inventions which were extensively used in later turbine practice.

Radial-flow turbines built

Turbine history would not be complete without some reference to the radial-flow steam turbine. Patent records contain a great many references to this type of prime mover. When Sir Charles Parsons started his steam turbine development he was a member of the firm of Clark, Chapman & Parsons in England. When he left this partnership to establish his own company, he was at first unable to recover the right to use his parallel-flow turbine patents. It was for this reason that a number of Parsons turbines of the radial-flow type were built prior to 1900. When he later recovered the right to use the parallel-flow principle, he returned to that type of bladed element which by this time offered the greatest promise for commercial development.

Many other attempts have been made to develop commercially the radial-flow construction. With steam admission at the inner radius, the natural increase in area as the steam flows outwards is a practical consideration of interest to the designer. The radial-flow principle has not been as extensively developed in the United States as in Europe.

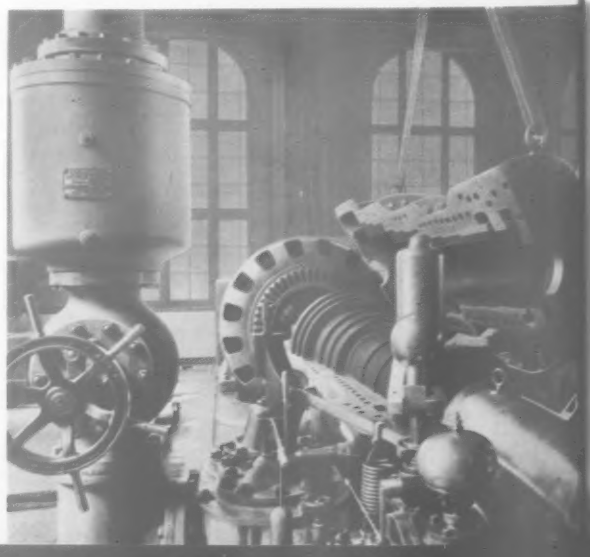
Progress made with blade contours

Turbine development up to 1900 included a wide range of blade or bucket shapes. Many inventors attempted to use open buckets, generally designated as the Pelton type. These could either be machined on the outer rim of the wheel or assembled as separate elements. Riedler-Stumpf turbines built in Germany are early examples of this type of bucket. Similar turbines were also built by Rateau, Zoelly in Switzerland, and many others.

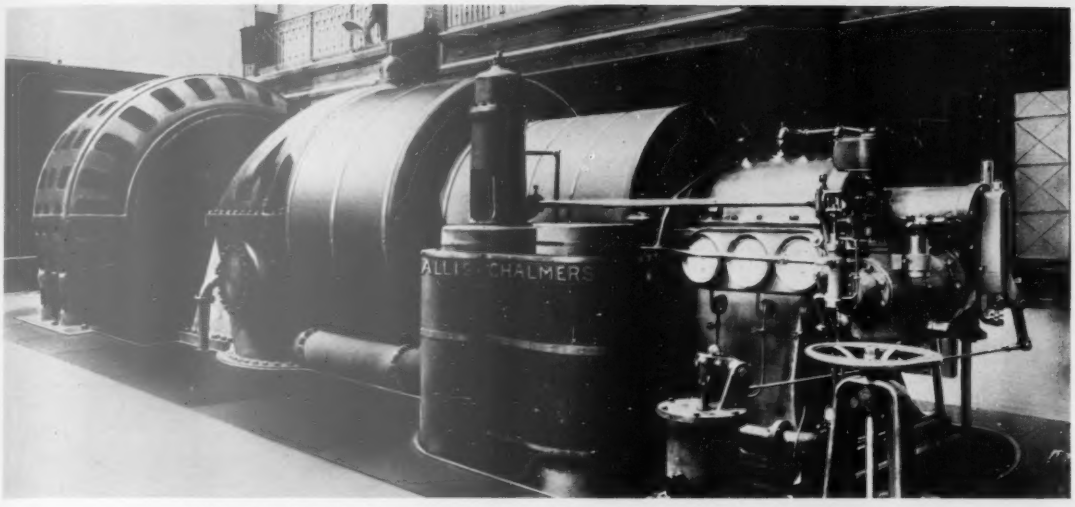
In the United States this open type of bucket construction is retained today in the Terry turbine for mechanical drive service. The passage of steam in a turbine of this type is shown in Figure 11.

The original Kerr turbine developed for mechanical drives in the United States also employed Pelton buckets.

Axial-flow impulse buckets developed by Rateau were formed of uniform thickness sheet metal. The efficiencies were found to be improved by the use of blade sections thickened at their mid-points to form approximately con-



STEAM TURBINES made their real start in power generation at the beginning of this century. This 1500-kw, 1800-rpm unit was installed in 1905 in the Washington Street Station of Utica, N. Y., Gas and Electric Company. **(FIGURE 6)**



BIG FOR ITS DAY, 5500-kw, 750-rpm, 25-cycle unit was installed in 1905 in the Kent Avenue (now Williamsburg) Station of the Brooklyn Transit Development Company. **(FIGURE 7)**

stant width passages. These principles were well recognized by the early 1900's, so that impulse blades of forms approximating those in use today began to appear.

The reaction turbine work of Parsons included some early attempts to use uniform thickness sheet metal blade sections, but he soon adopted well-formed crescent-shaped blades which provided better fluid flow characteristics for both stationary and moving elements.

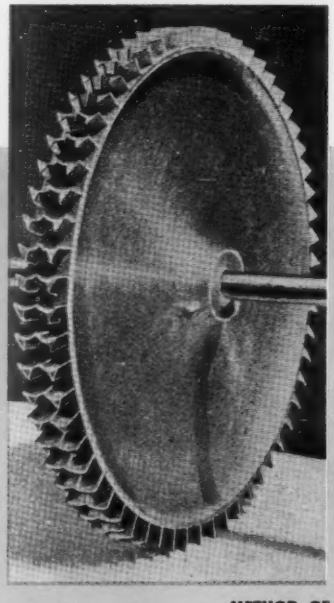
First marine steam turbines appear

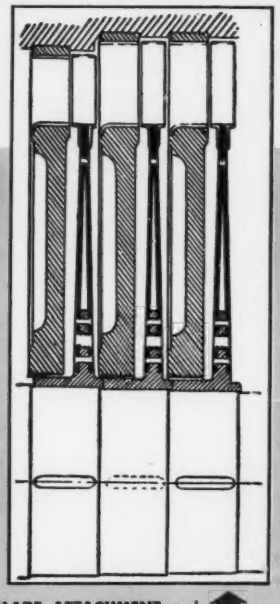
The first noteworthy marine application of steam turbines was on the 100-ft boat *Turbinia.* Due to patent complications, the original installation was built by Parsons with radial-flow turbines. In November of 1894, preliminary trials with a single 1780-rpm shaft resulted in a speed of 19¾ knots. The best result was obtained by the use of three propellers of the same pitch mounted in series on the same shaft.

Sir John Thornycroft and S. W. Barnaby had developed the initial theories of propeller cavitation during the trials of engine-driven British destroyers. Parsons then carried out the historic experiments to determine the cavitation characteristics of high speed propellers. He observed stroboscopically and photographed the cavitation voids of experimental propellers up to speeds of 2000 rpm. A wealth of valuable design information resulted from this research. One of his photographs is shown in Figure 12.

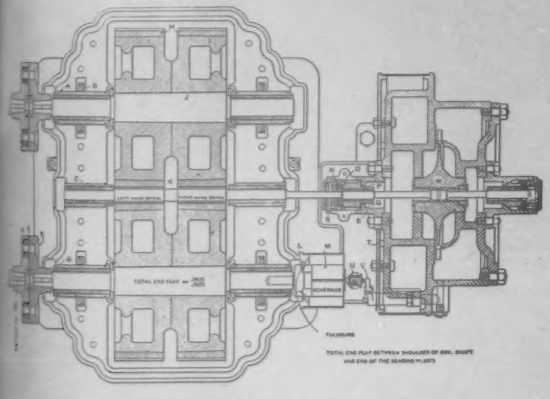
The second propulsion plant on the *Turbinia* included a three-shaft turbine arrangement, each shaft carrying

STEAM TURBINE WHEEL designed by Rateau similar to Pelton-type water wheel was experimented with. **(FIGURE 9)**

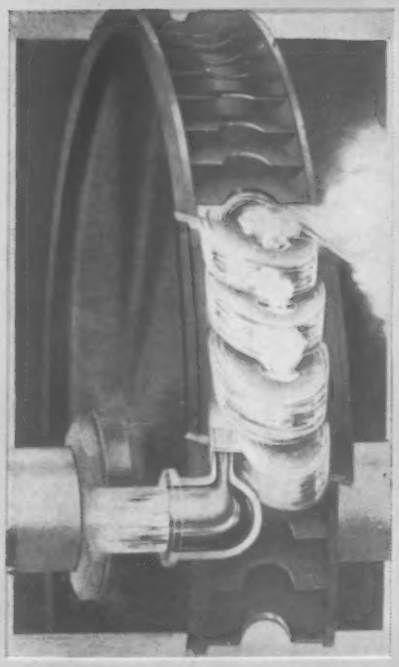




METHOD OF BLADE ATTACHMENT and disk design were features of early Rateau parallel-flow steam turbines. **(FIGURE 10)**

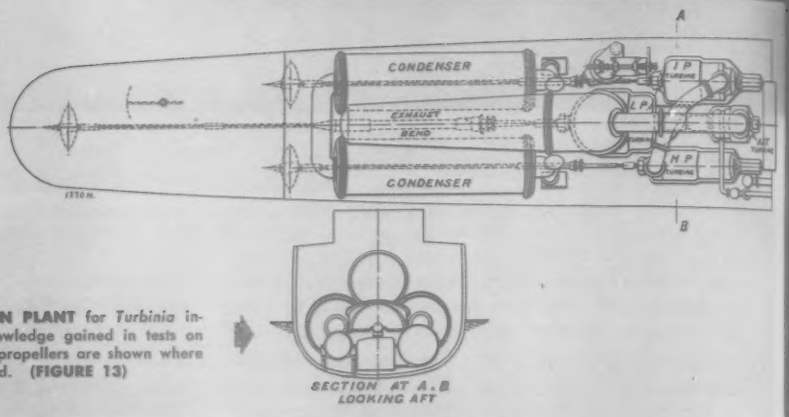


DE LAVAL employed turbine speeds up to 30,000 rpm with reduction gear ratios as high as 14 to one. Shown is a 300-hp turbine which ran at 10,600 rpm. **(FIGURE 8)**



TERRY TURBINE employs several reversing chambers for each nozzle. **(FIGURE 11)**

PROPELLER is stopped by means of a stroboscopic light for cavitation tests. These tests were made in 1895 with exposures of 1/3000th of a second. An arc light with a motor-driven shutter arrangement provided intermittent light. **(FIGURE 12)**



SECOND PROPULSION PLANT for *Turbinia* incorporated all the knowledge gained in tests on earlier models. Three propellers are shown where actually nine were used. **(FIGURE 13)**

three propellers. A high, an intermediate, and a low pressure turbine were used, each driving a separate propeller shaft. New propellers, proportioned from the results of cavitation research, were designed to turn at 2000 rpm. The new power plant with the parallel-flow turbines used steam at 157 psig. This arrangement is shown in Figure 13.

Figure 14 shows the *Turbinia* under way. Trials were carried out at record speeds up to 32¾ knots, with a reported top speed of 34 knots. The trials of the *Turbinia* were of further interest because the full power steam consumption of the turbine plant was found to be a few percent less than the consumption of corresponding reciprocating propulsion engines for high speed craft of that period. After the *Turbinia* trials, the application of turbines was continued in naval vessels and in ocean-going passenger ships. The high speed reduction gear had not yet come into its own, so that the early marine propulsion plants were direct-connected to the propeller shafts. The turbines in some of these early installations were of great size, and it is of interest to note the magnitude of the engineering problems involved in the construction of these direct-connected elements. The 180-rpm low pressure Parsons turbines on the original Cunard liners *Lusitania* and *Mauretania* were 184 inches in diameter over the tips of the low pressure blades. The completely bladed low pressure turbine spindles weighed 268,000 lb. The overall length of the low pressure turbine was 48 ft. Figure 15 shows the turbine equipment for the *Mauretania*.

Another notable installation was on the old White Star liner *Olympic*. A low pressure turbine of 16,000 shp received steam at 9 psia from reciprocating engines on the wing shafts. This low pressure reaction turbine operated with an exhaust vacuum of 28½ inches of mercury and the speed of rotation was only 165 rpm. The rotor tip diameter was 195 inches. The assembled low pressure spindle weighed 291,000 lb.

The forerunner of the modern geared marine propulsion plant is shown in Figures 16 and 17. This 10-hp marine turbine was built by the Parsons Marine Steam Turbine Company in 1897. The turbine had a 2-inch diameter spindle, which ran at a top speed of 19,600 rpm. The turbine was arranged to drive two reduction gears, each connected to turn a separate propeller at 1400 rpm. From this humble beginning the propulsion of ocean-going ships has been advanced to magnificent proportions, as illustrated by the geared steam turbine propulsion plant of the record-breaking luxury liner *United States*.

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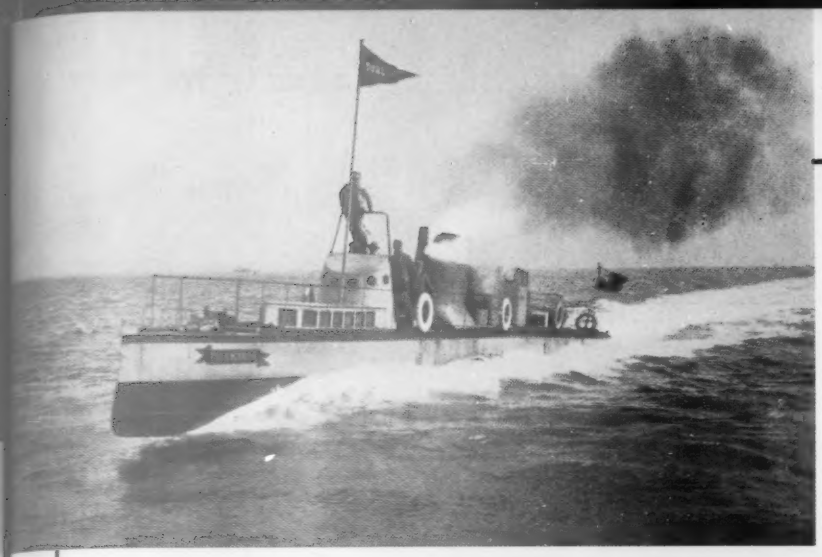
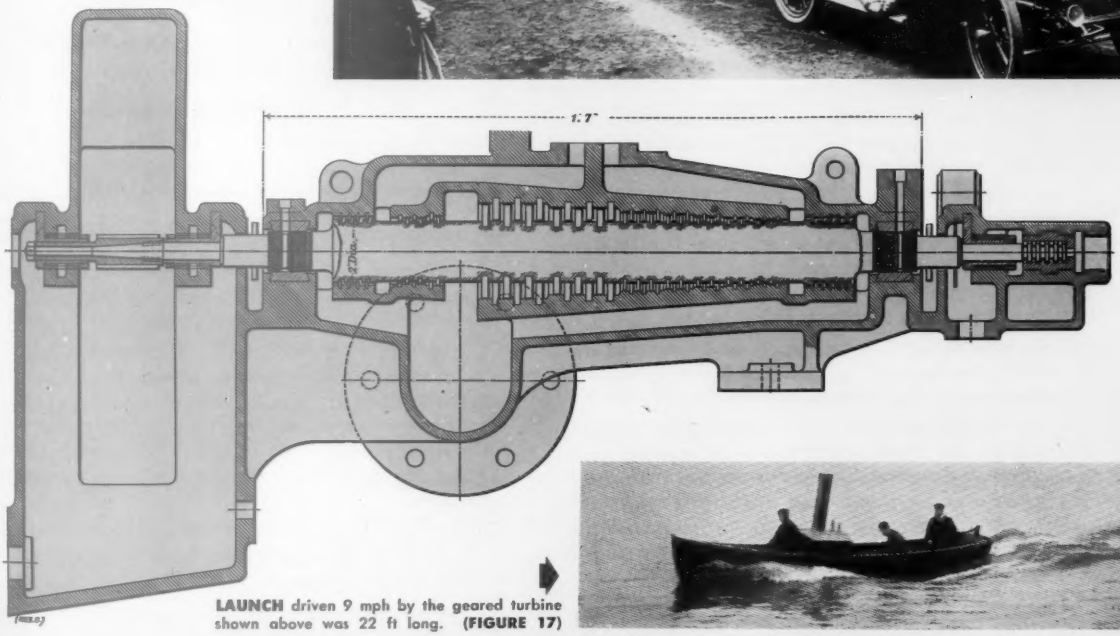


TRIAL PERFORMANCE of the *Turbinia* showed the many advantages of steam turbine propulsion. **(FIGURE 14)**

MASSIVE TURBINES built for the *Mauretania* in 1907 were rated approximately 70,000 shp. On trial runs this liner attained a speed of 26.06 knots. **(FIGURE 15)**

PIONEER GEARED TURBINE for marine propulsion made use of the most efficient turbine speed. This unit is now in South Kensington Museum, London, as is the second *Turbinia* propulsion plant. **(FIGURE 16)**



LAUNCH driven 9 mph by the geared turbine shown above was 22 ft long. **(FIGURE 17)**

Matching Individual Drives to the Torque Requirements of Specialized Machines is Demanded in Designing . . .

TODAY'S TEXTILE MOTORS

by **V. B. HONSINGER**
Engineer-in-Charge
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and

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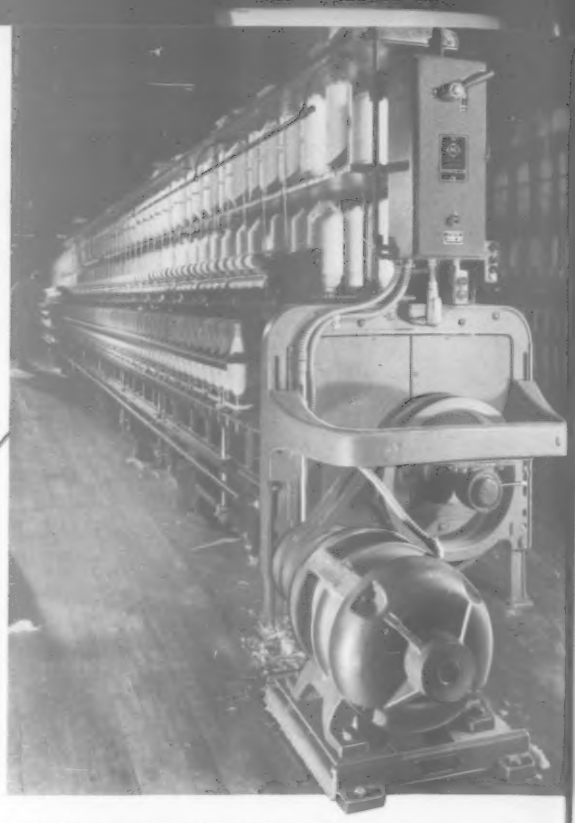


TYPICAL OF INDIVIDUALLY DRIVEN modern spinning frames, this machine is powered through a multiple V-belt drive by a textile motor that has wide-open construction for non-clog ventilation.

THE TEXTILE ART is one of the oldest known to mankind, and many a pictorial remnant of civilization as it existed at the dawn of history bears some evidence of the clothmaking art. Through the centuries, hand operations in spinning yarn and weaving cloth have given way to machines, and machinery for each operation has constantly improved.

Hand looms were long ago replaced by power looms, and these in turn gave way to the present-day automatic looms. Similarly, the spinning wheel of colonial times has evolved to modern, high speed ring spinning. The machinery for each phase of textile manufacture has undergone tremendous development, and even more recently a similar evolution has occurred in the drives to power them.

As late as the turn of the last century, many mills were operated by water power, although steam power plants were predominant. Prime movers were connected to individual machines through overhead line shafting, pulleys and a forest of long flat belts.

About 1910, when the textile industry began to demand machines that could operate at higher speeds without sacrificing product quality, one of the first steps was development of individual drive motors. Although it was believed by many that general purpose electric motors would be satisfactory for all textile applications, in actual use these motors proved inadequate.

To begin with, torque requirements differed between various makes and types of textile machines — all of which operated in highly humidified, lint-laden atmospheres. In addition, floor space was at a premium in textile plants constructed prior to about 1940 because machines were placed close together to hold down line-shaft lengths. After general purpose motors had been tried and found wanting, mill operators, machinery builders, and motor manufacturers combined their research and engineering facilities to develop motors for each application.

Motor drives for spinning frames

Among the first of the textile motors to be developed was the *Quick-Clean* motor for spinning frames and twisters. These motors drive machines that operate in rooms that have lint-laden atmospheres of about 60 percent humidity and temperatures of 25 to 30 C.

When applied to these machines, general purpose motors became clogged with lint, overheated and caused fire hazards. Efforts to exclude lint by means of screens proved ineffective, since the screens became clogged.

Consequently, motors were developed to alleviate lint collection. Contours were streamlined. Special fans were developed to throw off lint. Equally important, end turns were provided with molded coil shields or surrounded with thermosetting compounds to streamline these parts.

Motors of this type have driven spinning frames and twisters very successfully for many years. Recently, newer types of spinning frames have been developed with larger

"packages," that is, more yarn on a single bobbin. These frames require from 10 to 20 horsepower, depending upon the number of spindles, yarn size, and spindle speed. On any given frame, the horsepower required when bobbins are full is much greater than when the bobbins are empty.

In this case, space limitations required a motor small in physical size, comparable to a 288 NEMA frame, yet capable of developing up to 20 horsepower, 1740 rpm. Figure 1 illustrates the streamlining of coil ends and other parts in a 20-hp open motor of special design which has high efficiencies and a smaller than usual frame size. Characteristics of this motor are indicated in Figure 2. Efficiency was made as flat and high as possible, averaging around 92 percent, in the 10 to 20-horsepower range.

Temperature rise was a design consideration because it affects motor life, particularly insulation life. Figure 3 indicates temperature rise over a 15-hour period. During this interval the bobbins progress from empty to full and load increases from 10 to 20 horsepower. This is one of the more severe tapered load applications, and temperature rise was well within satisfactory limits.

Drives developed

With specially designed motors for spinning machines available, the next step was development of individual belt drives. Flat belts from line shafts gave way to rope drives on the short centers made possible by mounting the drive motors on the driven machines. Rope drives proved unsatisfactory because of short belt life.

Since sudden starts or shock loads might break or thin out the stock being processed, a drive that would start smoothly and transmit the required horsepower without transmitting shock was the subject of considerable research. This research resulted in the multiple V-belt drive. High inertia loads were started smoothly because of the "give" in the belts and because the contact between sheaves

WIDE-OPEN CONSTRUCTION and streamline-covered end turns permit lint-laden air to flow right through this 20-horsepower, highly efficient motor which has an attached brake. **(FIGURE 1)**

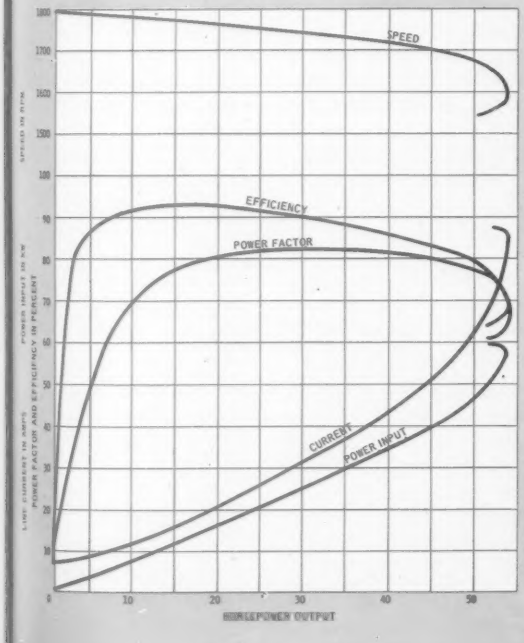
and belts permitted a certain amount of slip. By using multiple groove sheaves and parallel belts, a drive to transmit the required horsepower without transmitting shock could be designed.

After some years of experience in applying drives to textile machinery, the variable pitch sheave was developed to fill the need for a variable speed drive from a constant speed motor. These sheaves were developed in both stationary and motion controlled types.

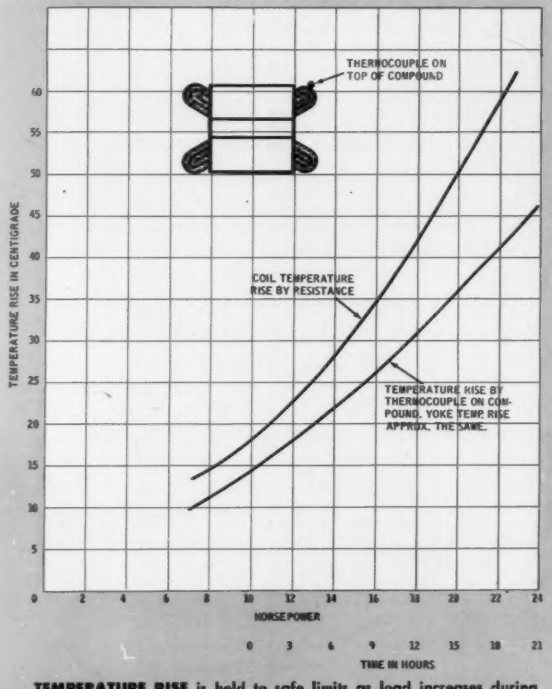
Individual motors for looms developed

The weave room, where strands of warp and filling are woven into cloth by looms, is one part of the textile mill that has shown rapid evolution in recent years. Slow speed, low production machines have been replaced by highly intricate, precision machines that operate at high speed while producing a product of higher quality. The development of individual loom motors played an important part in this transition.

The loom itself has shafting, gears, and cloth rolls which rotate; a lay beam and harness which oscillate somewhat smoothly; and other parts like the picker sticks and shuttle, which have very abrupt motions producing shock forces.



MOTOR EFFICIENCY is as high as possible from 10 to 20 horsepower, the spinning frame tapered load range. **(FIGURE 2)**

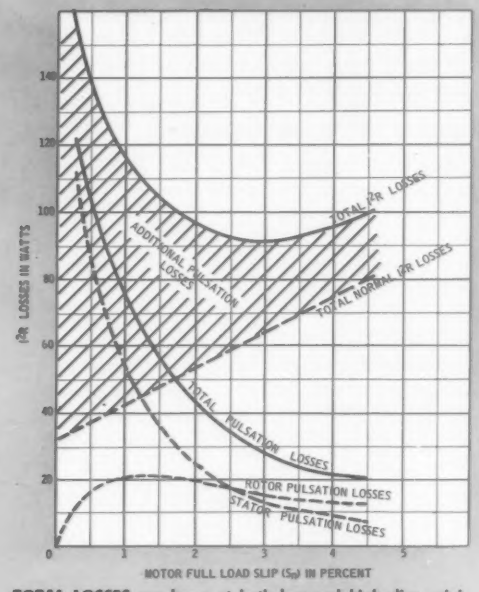

THERMOCOUPLE ON TOP OF COMPOUND
COIL TEMPERATURE RISE BY RESISTANCE
TEMPERATURE RISE BY THERMOCOUPLE ON COMPOUND. YOKE TEMP. RISE APPROX. THE SAME.
TEMPERATURE RISE IN CENTIGRADE
HORSEPOWER
TIME IN HOURS

TEMPERATURE RISE is held to safe limits as load increases during the 15 hours required to process the doff. **(FIGURE 3)**

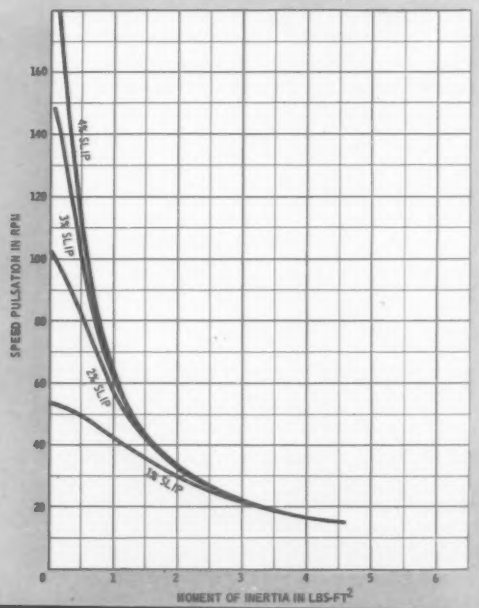
All the energy to drive a loom ultimately comes from the loom motor. Instantaneous peak load torque, however, is supplied from both motor torque and kinetic energy stored in the rotating and oscillating parts of the system. Since power requirements fluctuate rapidly, one or two hundred times a minute according to the speed of the loom, if a loom motor is not properly designed, the loom frequently drives the motor through a part of the weaving cycle and large speed pulsations result. For this reason, loom motors are oversize electrically, or have added rotor Wk^2.

The factors affecting speed pulsation most are found in the loom itself. First, the loom size, as determined by oscillating moment of inertia, directly increases speed pulsations. Second, and most important, the speed of the loom crankshaft increases the speed pulsation approximately as the square of rpm. Consequently, a small high speed loom can overheat a motor as easily or easier than a large low speed loom.

Injudicious speeding-up of looms without regard for the drive motors can result in motor breakdown. An increase in speed pulsation at the same average horsepower output (1) increases rms amperes per phase, (2) increases average watt input, (3) increases motor losses, (4) increases rms slip without appreciably affecting average speed or slip, and (5) decreases average efficiency and power factor.



TOTAL LOSSES are large at both low and high slips, minimum at 3 percent slip for a typical loom motor. **(FIGURE 4)**



SPEED PULSATION is almost independent of motor slip, if 2 percent or higher, at moments of inertia above 1 lb-ft². **(FIGURE 5)**

Slip is built in

The amount of slip built into a loom motor depends on the induced pulsation losses and how they vary with motor slip at a fixed load torque. For a given output, the normal I^2R losses increase almost linearly with slip, whereas the pulsation I^2R losses decrease almost inversely with slip. The total losses, therefore, are large both at low slips and very high slips, passing through a minimum at intermediate values. Figure 4 shows an I^2R loss breakdown for a typical loom motor. In this case, total I^2R losses are minimum at 3 percent slip.

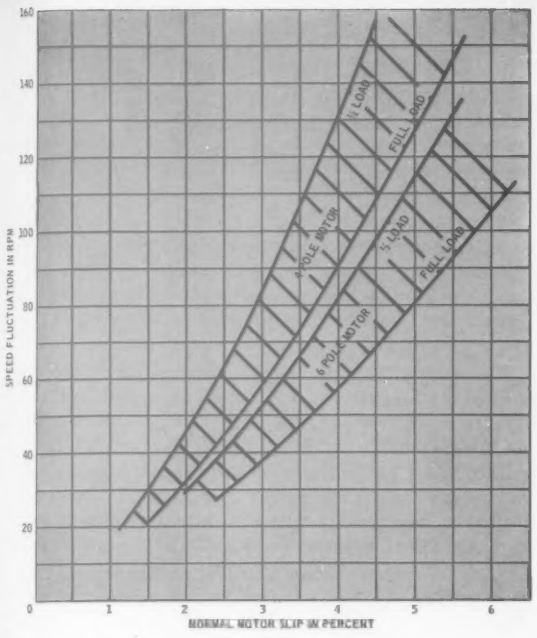
Largest inertia component in the loom system is the motor moment of inertia, all loom constants being referred to the motor shaft. Hence, practically the entire suppression of speed fluctuation must be performed by the motor rotor. This speed fluctuation is about inversely proportional to the motor moment of inertia. Figure 5 indicates that for slips .02 and higher and for moment of inertia of one lb-ft² or more, speed pulsation is fairly independent of slip. However, proper selection of motor slip is important in minimizing I^2R losses and temperature rise.

Exactly how much flywheel effect to use in a loom motor is not well defined by any specific rule. Selection is a matter of judgment and trial. However, once the inertia constants are selected, there are pairs of values for the motor's moment of inertia and normal slip which are best, since they give minimum losses for a particular loom load. Figure 4 relates to a particular case involving a 4-pole motor in which 3 percent slip produced minimum losses when the loom and motor inertia constant was one lb-ft². The motor was fully loaded on an average horsepower output basis and regenerating slightly. Maximum speed was 1807 rpm and loom picks averaged 174½ per minute. This is typical of many loom loads for 1-horsepower, 4-pole motors. Figure 6 indicates the normal slip which produced minimum losses for various speed fluctuations and average load.

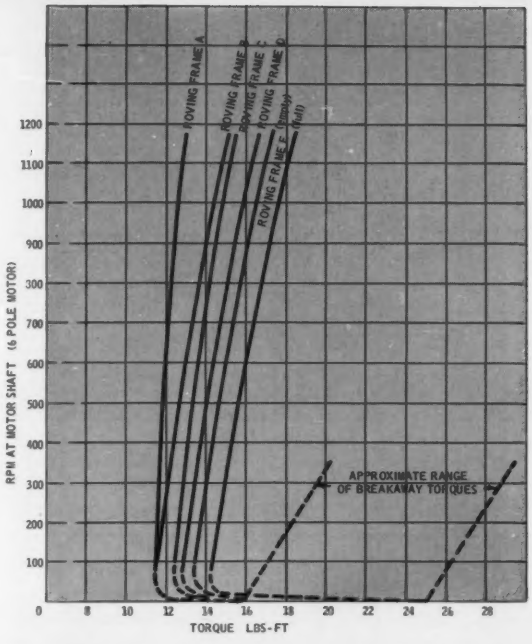
Special loom motors for this duty are typified by the one shown in Figure 7. Enclosed frame construction and rugged motor parts are important, because of severe vibration and shock loads.

Slow-start roving frame motor developed

After satisfactory individual drives for spinning frames and looms had been developed, individual motors to drive



PROPER SLIP to give minimum losses for a 1-horsepower, 550-volt, 3-phase, 60-cycle loom motor with Wk^2 of about 1 lb-ft^2 or more is indicated by the shaded areas. **(FIGURE 6)**



STARTING TORQUE built into roving frame motors must exceed breakaway torque of roving frames and be adaptable to the varying torque requirements of different frames. **(FIGURE 8)**

roving frames became the subject of research. Again there was a specific problem — one that could not be satisfactorily answered by either of the two special textile motors so far developed or by a general purpose motor.

Roving frames take the extremely tenuous cotton slivers and draw them out into longer and thinner strands while imparting twist necessary for the additional strength that assures uniform drafting in the spinning operation. When handling such extremely tenuous material, it is very important that the frame start slowly and accelerate smoothly to full speed. Any sudden jerk causes broken ends and consequent production loss. Even if the ends do not break on the roving frame, they will contain thin places, or uneven roving, which ultimately break down in spinning and cause lost production.

Roving frames of many different sizes are required to make the different sizes and weights of roving; but by far the most common size is the 10 by 5-inch, 120-spindle per frame unit. This size is used in nearly 85 percent of the installations. Data from a number of machines of this size indicated these average requirements:

DESIGNED TO WITHSTAND severe vibration and shock loads, this loom motor has enclosed frame construction and rugged motor parts. **(FIGURE 7)**

Moment of inertia is about 15 to 19 lb-ft^2 at the 1150-rpm motor shaft. Steady-state horsepower requirements vary somewhat with make but fall between three and four horsepower. Load increases approximately 6 percent as bobbins progress from empty to full. Breakaway torque at the motor shaft varies from 12 to 25 lb-ft, and may vary as much as 25 percent on the same make of machine.

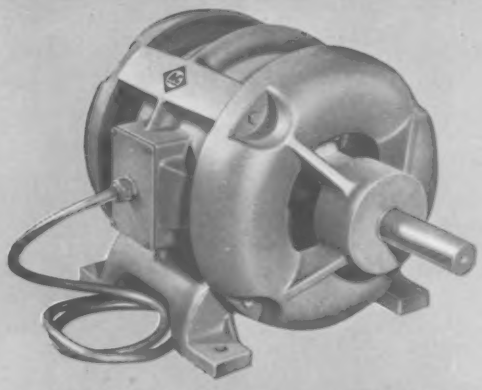
Considerations in addition to torque requirements include lint-laden atmospheres with 20 to 30 C ambient temperature and about 60 percent relative humidity, and a duty cycle that is continuous and nearly unfluctuating.

Sliver is converted to roving by the roving frame in about ¾ to 1½ hours. The motor is then stopped, bobbins changed and the motor started again. If the roving strand breaks during the starting period, the frame has to be shut down and the break repaired. Breaks mean lost production.

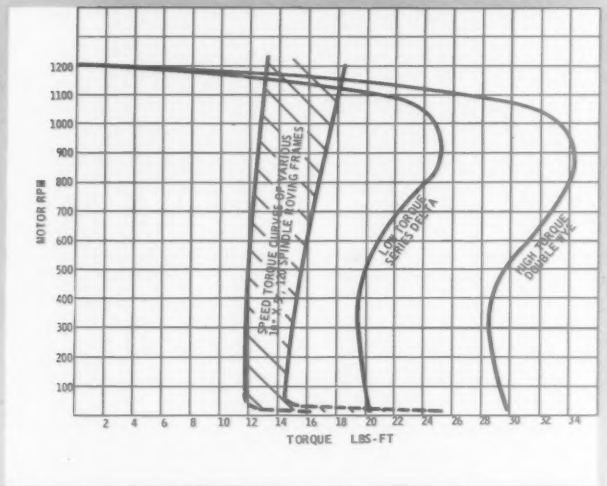
Since it is sometimes necessary to slow down the roving machine during the starting period, the speed-torque curve of the motor must parallel the roving frame speed-torque curve and approach it as closely as possible. However, there are two contrary limitations to this goal.

First, so that the frame will start, the motor starting torque must exceed roving frame breakaway torque, indicated by the sharp spur at zero speed on the roving frame curve in Figure 8. Second, variations between roving frames themselves mean that there is not one speed-torque curve but several, each similar to the other but with different torque values.

Based on experience, some roving frame manufacturers recommend a 5-second acceleration period as sufficient to



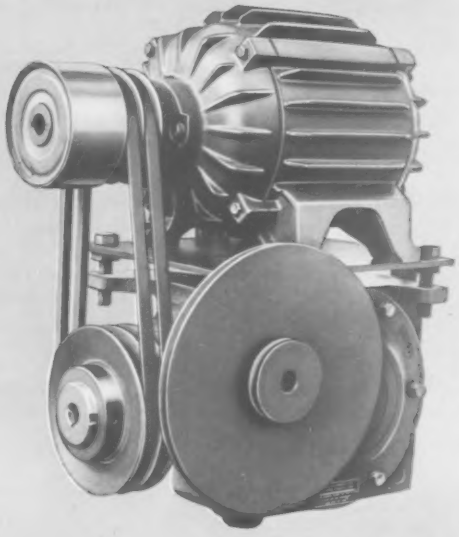
THIS TYPICAL roving frame motor has one winding that can be connected either of two ways for matching torque to load. **(FIG. 9)**



ROVING FRAME MOTORS are designed so that their speed-torque curves nearly parallel those of roving frames. **(FIG. 10)**

NORMAL REQUIREMENTS for a 50" dia. 40" wide cotton card. **(FIG. 11)**

	At Motor Shaft 1740 RPM	At Card Shaft 165 RPM
HP Required	1.0 to 1.2	1.0 to 1.2
Moment of Inertia	54 to 63 lbs-ft^2	6000 to 7000 lbs-ft^2
Breakaway Torque	7.1 to 9.5 lbs-ft	75 to 100 lbs-ft



alleviate the breaking of roving ends. Acceleration period, the time that elapses between instant of start and steady-state running condition, can be measured fairly accurately by using oscillographic techniques to determine when both speed and ampere values become constant.

From the curves in Figure 8, it can be determined that an average motor torque of 23 lb-ft will give a 5-second acceleration period for the frame easiest to start. This value is used as the design characteristic for the low torque connection of a specially designed one-winding motor that can be reconnected for about 33 percent higher torque.

This roving machine motor is shown in Figure 9. It is rated 3 hp, 1150 rpm, 3 phase, 60 cycle, and built in a 254 frame. Windings can be connected either series delta for low torque or parallel wye for high torque. When installed, this motor is first connected for low torque, then reconnected for high torque if it fails to start. Sometimes, if applied to a new machine, it is advisable to reconnect the motor from high to low torque after a "wear-in" period.

Figure 10 shows the torque curve of this motor. It is as close to the roving frame speed-torque curve as feasible. The average motor torque is 21 lb-ft for the low torque winding and 29.5 lb-ft for the high torque winding. Usually this results in at least a 5-second acceleration period and alleviates the breaking of roving ends. No special torque reducing resistors or special motor starters are needed. Five-horsepower motors at 1200 rpm are also made with similar characteristics.

Card drives developed

The development of individual drives for textile processing machinery followed a pattern almost directly opposite to the flow of raw material. One of the first processes — that of taking the loose or open-blended cotton lap, pulling out most of the foreign substances, disentangling and paralleling the fibers — is performed by the cotton card. Yet, only after individual drives had been developed for spinning machines, twisting machines, looms and roving frames, was an effort directed toward bringing the card room out of its forest of overhead shafting, belts and pulleys. Cramped space was one reason why the card room had been more or less ignored for years. Floor space had traditionally been more at a premium here than in any other part of the textile mill.

For several years many and varied types of card drives have appeared in more or less experimental stage, but the majority of them required too much space. Consequently, very few have been successful. Those drives that complied with space limitation were supported by the card arch, and in time proved detrimental to good carding practice because excessive vibration damaged the card clothing and flats.

Cotton cards, in their basic size and design, have remained unchanged for about 50 years. Regardless of make or date of manufacture, a cotton card nearly always has a 50-inch diameter main drive cylinder that is 40 inches wide. This cylinder is covered with small pinlike wires, called clothing, which enable the card to perform its function and to deliver the stock to the drawing can in continuous "sliver" form.

High inertia is the main characteristic of a cotton card and causes long starting periods of about 30 to 40 seconds.

DESIGNED TO MOUNT directly on the card cylinder shaft, this drive is compact . . . does not distort the gap between flats and main card cylinder. **(FIGURE 12)**

Nearly all cotton cards run at speeds from 165 to 175 rpm. The individual drive motor is usually a 1½-hp, 1740-rpm, 3-phase, 60-cycle induction motor. A gear or belt system, or some combination of these, is required to reduce the motor speed to the low card speed.

Clearance between the flats and main cylinder is only .007 to .010 inch. Any individual drive must be mounted in such a way that this dimension is not affected. Mounting a drive on the card arch, for example, causes a reactive torque to be induced in the arch by the motor torque. This is often sufficient to distort the air gap between flats and the main cylinder. Other requirements of a card drive are:

(1) It must be suitable for lint-laden atmospheres.

(2) It must be reversible for grinding. This can be accomplished by a simple reversing switch on the motor or by a reversing magnetic motor starter.

(3) It must be provided with means for stripping. This may be accomplished by a step pulley on the gearbox. There is, however, a trend toward use of small gearmotors, similar to those used on electric drills, to drive the stripper rollers. Where stripping pulleys are used on the gearbox, each and every card needs this provision.

Figure 11 gives the normal horsepower, moment of inertia and breakaway torque of the cotton card.

Individual drive for cotton cards

Recently, the card drive shown in Figure 12 has been developed. It has a 1½-hp, 1740-rpm motor mounted on a gear, and the gear in turn has a hollow low speed shaft which slips over the card shaft. The high speed gear shaft is driven through V-belts. Gear ratio is 6.917 and belt ratio is 1.5, giving a total ratio of about 10.4.

A single reduction, right angle spiral gear is used so that the motor can be mounted crosswise on the card shaft, and total extension into the aisle is determined by motor width rather than the greater motor length. This conserves aisle space and permits adequate clearances despite cramped floor space.

Mounted on the motor shaft is a clutch which minimizes current demands during the long starting period and reduces motor temperature rise while stripping. In addition, the clutch makes available maximum motor torque, instead of the smaller starting torque, for breaking the card away and setting it in motion. Breakaway torque of a card is relatively severe, and the additional torque afforded through utilization of motor maximum torque is desirable.

This drive has no connection whatever with the card arch frame, but is mounted directly on the card cylinder shaft. It is impossible for this drive to cause distortion at the air gap underneath the flats.

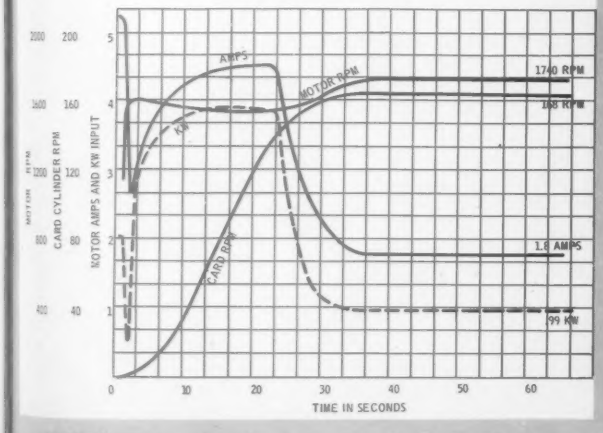
Figure 13 shows the conditions existing during the starting of a typical sleeve bearing card driven through a clutch by a 1½-hp, 1740-rpm motor. This curve has three distinct regions: (1) the breakaway region during the first few fractions of a second, (2) the normal acceleration region of about 30 to 40 seconds, and (3) the steady-state region.

When started, the motor comes up to speed corresponding to maximum torque almost instantly. The card speed, however, is very low — nearly zero — indicating the severity of breakaway torque. Maximum motor torque is very desirable at this stage. As soon as the card is set into motion, torque requirements decrease rapidly and the card comes up to speed.

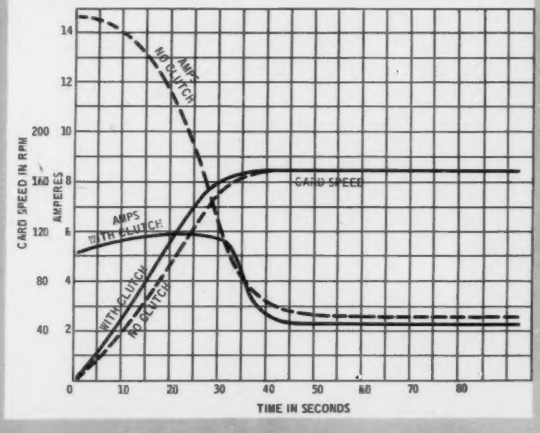
A comparison of two drives, one using a clutch, the other using no clutch but otherwise identical, is graphed in Figure 14. The greatly decreased magnitude of current as well as I^2R losses in the motor when using a clutch is evident. Because maximum motor torque is available to start the card, the card is brought up to speed faster when the drive having a clutch is used.

Despite its antiquity, the textile industry is dynamic, growing — constantly improving its techniques and machinery. Manufacturing costs are being lowered, quality is being improved, and variety of products increased.

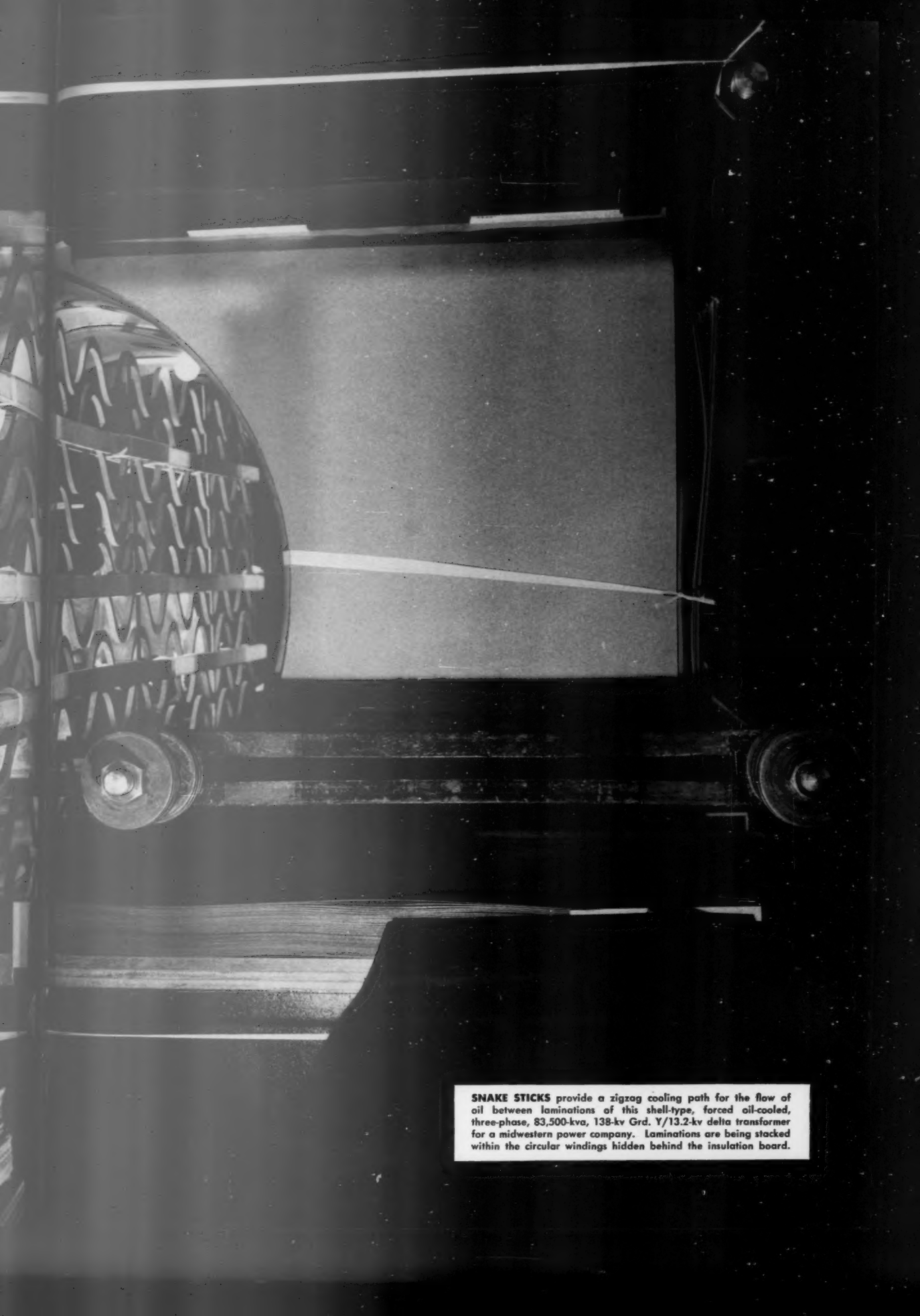
As this trend continues, new motors and new drives will need to be developed. Regardless of new developments, the basic characteristics inherent in textile machinery and textile mills will always be factors in their design.



BREAKAWAY, normal acceleration, and steady-state conditions encountered when starting a cotton card are indicated. **(FIGURE 13)**



CURRENT INPUT is reduced and maximum motor torque is made available for card breakaway by adding a clutch. **(FIGURE 14)**



SNAKE STICKS provide a zigzag cooling path for the flow of oil between laminations of this shell-type, forced oil-cooled, three-phase, 83,500-kva, 138-kv Grd. Y/13.2-kv delta transformer for a midwestern power company. Laminations are being stacked within the circular windings hidden behind the insulation board.

Simplifying Coordination of Short Circuit Protection

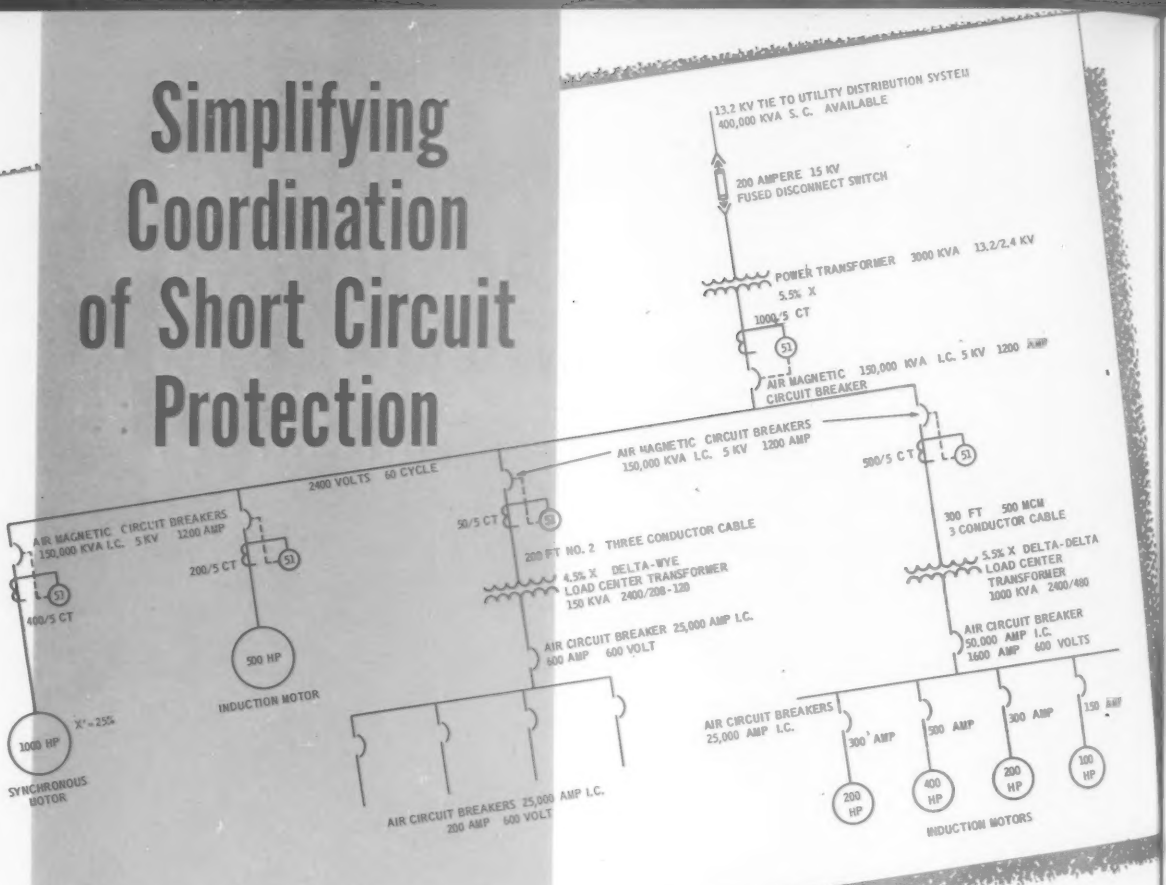


FIGURE 1

by **R. LOEWE**
and
PETER F. LOVATO
Switchgear Section
Allis-Chalmers Mfg. Co.

Here are easy steps you may use to find correct relay settings for most industrial distribution systems.

ADJUSTING THE TRIP SETTINGS on protective devices and relays in radial distribution systems such as found in most industrial switchgear installations is a problem of coordination. Various instruction books, textbooks, catalogs, and technical articles contain so much information on the procedure for obtaining proper settings that anyone trying to do the job right is apt to become confused. Here is a simple method using only the essential steps in obtaining the trip settings for the various protective devices from the utility line down to the individual load center feeders. While certain mathematical formulas are necessary, their solution is quick and easy.

The first step before any calculations are attempted is to draw a simple diagram of the system to clearly show the problem. Figure 1 shows a typical system such as found in many small or medium size industrial plants. This system, which has a single main transformer with a 2400-volt secondary, one main secondary breaker and four distribution feeders, is used to demonstrate relay coordination. Two of the feeders supply motors; the other two, load center substations.

After the single-line diagram is prepared, pertinent facts are added to various parts of the system. The first item is the available short circuit at the primary of the main power transformer. This information is generally obtained from the power company and is given as a certain number of kva at a given voltage.

Impedances are noted

Next the impedance of each transformer is added to the diagram. This information is generally given on the transformer nameplate or may be taken from test data if available.

The impedance, expressed in percent, roughly equals the percent voltage change in the transformer secondary resulting from a change of secondary load from zero to full load while rated primary voltage is maintained. A 2400/480-volt transformer having 5 percent impedance will have a secondary voltage reduction of approximately 0.05 x 480 volts or 24 volts at full load. Hence the secondary voltage of the transformer used as an example in Figure 1 will become 456 volts.

For the purpose of setting protective devices, the effect of induction motors on the system may be neglected. The effect of synchronous motors on the system, however, is a different story. Here the motor transient reactance must be used. This can be obtained from the motor manufacturer or may be approximated from values published in available tables. These are expressed in terms of the motor kva rating. The motor kva rating may be found from the horsepower by the following formula:

$$(1) \qquad \text{kva} = \frac{\text{hp} \times 0.746}{\text{eff} \times \text{pf}}$$

The horsepower and power factor are generally found on the machine nameplate. The efficiency usually can be assumed to be approximately 90 percent.

In this example a power factor of 90 percent as well as an efficiency of 90 percent is assumed and the kva will be

$$\frac{1000 \times 0.746}{0.9 \times 0.9} \quad \text{or 920 kva}$$

Calculations converted to common base

In order to convert this value into a usable form it must be changed to an equivalent on a 3000-kva base. For convenience, the kva base used is the kva of the largest transformer on the system. In this case, it is a 3000-kva transformer.

$$(2) \qquad \frac{\text{kva device}}{\text{kva base}} = \frac{\text{percent reactance of device on its own base}}{\text{percent reactance of device on the new base}}$$

or in this example (assume motor to have 25 percent transient reactance):

$$\frac{920}{3000} = \frac{25\%}{X}$$

$$X = \frac{3000}{920} \times 25 = 81.5 \text{ percent reactance for synchronous motor on 3000-kva base}$$

The next step is to label the cable system connecting the various pieces of equipment. The usual data tables for cables give the resistance and the reactance in ohms per thousand feet or per mile. At 2400 volts and higher, the reactance is used, while at 600 volts or below the impedance should be used since resistance becomes an appreciable factor. The impedance is the square root of the sum of the squares of the resistance and the reactance. A cable with 0.031 ohms reactance and 0.121 ohms resistance would have its impedance calculated as follows:

$$(3) \qquad z = \sqrt{r^2 + x^2}$$

$$z = \sqrt{(0.121)^2 + (0.031)^2} = 0.125 \text{ ohms}$$

Since the calculations of our example will be based on the addition of impedance values of the various parts of the system, the impedance of cables must be converted to a form comparable to the percent impedance of the transformers.

The formula for percent reactance is:

$$(4) \qquad \%X = \frac{\text{ohms} \times \text{kva}}{10 \times \text{kv}^2}$$

In this formula kva represents the kva base referred to in formula 2. To convert the cable reactance, formula 4 is applied.

$$\frac{0.125 \times 3000}{10 \times (2.4)^2} = 6.5\% \text{ reactance}$$

This means that if the cables carry 3000 kva at 2400 volts the voltage will drop 6½ percent, or 156 volts below the 2400 rated volts.

In our example, however, there is a 300-foot 500 MCM cable feeding the 1000-kva load center transformer at 2400 volts. Neglecting resistance, the approximate reactance of 500 MCM, 3/C cable = 0.128 ohm/mile.

$$X = 0.128 \text{ ohm} \times \frac{300 \text{ feet}}{5280 \text{ feet/mile}} = 0.00726 \text{ ohms for 300 feet}$$

using formula 4

$$\frac{0.00726 \times 3000}{10 \times (2.4)^2} = 0.30\% \text{ reactance on 3000-kva base}$$

In the system shown in Figure 1, there are two load center transformers, a 150 and a 1000 kva. The impedances of these transformers are given with respect to the individual transformer ratings. To use these values, the same conversion is used as for the synchronous motor.

For the 150-kva transformer, using formula 2:

$$\frac{150 \text{ kva}}{3000 \text{ kva}} = \frac{4.5\%}{X\%}$$

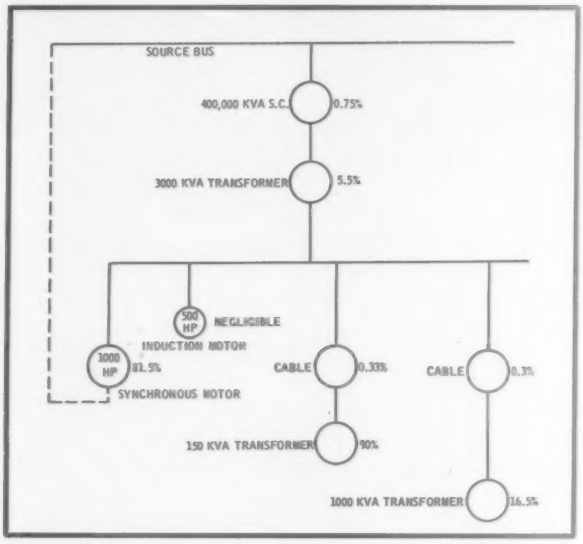
$$X = \frac{3000}{150} \times 4.5\% = 90\% \text{ reactance on 3000-kva base}$$

For the 1000-kva transformer, using formula 2:

$$\frac{1000}{3000} = \frac{X\%}{5.5\%}$$

$$X = \frac{3000}{1000} \times 5.5\% = 16.5\% \text{ reactance on 3000-kva base}$$

There is one remaining item to convert in this sample system. The maximum short circuit available at the main transformer primary has been given in kva. This value must be reduced to a percentage on the system base, which



REACTANCE VALUES are shown for each motor, line and transformer. This procedure aids in visualizing the problem. **(FIGURE 2)**

is 3000 kva. To do this the following formula is used:

$$(5) \qquad \%X = \frac{\text{base kva} \times 100}{\text{available short circuit kva}}$$

$$\%X = \frac{3000 \times 100}{400{,}000} = 0.75\%$$

The short circuit at the primary of the main power transformer may be given as a value of short circuit current in amperes at the supply voltage. If this is the case, the current is multiplied by the line-to-line voltage and by the square root of three. In simple terms, amperes $\times$ volts $\times \sqrt{3} =$ the kva value. At this point all the values necessary for calculating short circuit currents at various points in the system are available. All of these values are placed on the reactance diagram shown in Figure 2.

All short circuit contributions are considered

The diagram clearly shows two sources of short circuit current: One is the utility system and the other the synchronous motor. The short circuit current at any point in the system then is the sum of the values contributed by these two sources. The reactance diagram is arranged to show all of the short circuit sources connected to the source bus. In the case of the synchronous motor, the dotted line shows this connection. In order to find the short circuit current at any point in the system, the reactances between the source bus and the point of short circuit are combined in the same manner as the ohmic values in a direct current circuit. To do this, the ordinary relationships for series and parallel combinations of resistors are used.

For a fault on this 2400-volt bus the total short circuit current would be equal to the sum of the contributions from the power supply and the synchronous motor. That portion from the power supply would be found by adding 0.75 percent for the power supply to 5.5 percent for the 3000-kva transformer, giving a total of 6.25 percent. This can be changed to an equivalent value of short circuit kva by adapting formula 5 to the unknown quantity, as follows:

$$(6) \qquad \text{short circuit kva} = \frac{\text{base kva} \times 100}{\text{percent reactance}}$$

$$\text{short circuit kva} = \frac{3000 \times 100}{6.25\%} = 48{,}000 \text{ kva}$$

The contribution from the synchronous motor can be obtained in the same manner:

$$\text{short circuit kva} = \frac{3000 \times 100}{81.5\%} = 3680 \text{ kva}$$

The total short circuit kva on the 2400-volt bus would then be 3680 kva plus 48,000 kva or 51,680 kva.

For this example it is also necessary to find out what the short circuit kva will be at the primary terminals of the load center transformers as well as at their secondary terminals. The first step will be to take the total short circuit kva at the 2400-volt bus and convert it from kva to percent reactance. In this case, using formula 5:

$$\%X = \frac{\text{base kva} \times 100}{51{,}680} = \frac{3000 \times 100}{51{,}680} = 5.8\%$$

To find the short circuit kva at the primary terminals of the 1000-kva transformer, 5.8 percent is added to the cable reactance of 0.30 percent, previously calculated, for a total equivalent reactance of 6.10 percent. Applying formula 6 results in an equivalent short circuit kva of 49,200 kva found as follows:

$$\text{short circuit kva} = \frac{\text{base kva} \times 100}{\%X} = \frac{3000 \times 100}{6.1\%}$$

$$= 49{,}200\text{-kva short circuit at primary of 1000-kva transformer}$$

In a similar manner, the transformer reactance is added to the 6.1% to obtain the short circuit current at the secondary terminals:

$$6.1\% + 16.5\% = 22.6\%$$

$$\text{short circuit kva}_{\text{secondary}} = \frac{\text{base kva} \times 100}{\%X} = \frac{3000 \times 100}{22.6\%}$$

$$= 13{,}270\text{-kva short circuit at secondary of 1000-kva transformer}$$

In the same way, a value of 49,000 kva is obtained at the primary terminals of the 150-kva transformer and 3120 kva at its secondary terminals.

The values of short circuit kva can be converted to short circuit current at the respective fault points by the use of the following formula:

$$(7) \qquad I = \frac{\text{kva}}{\sqrt{3}\,E}$$

where $I =$ short circuit current, kva = short circuit kva, and $E =$ line-to-line kilovolts at the particular point in the system where the protective device which we are going to set is located.

Trip settings determined

The correct trip settings for the system breakers and their relays can now be determined from the data obtained above. The circuit breaker nearest the fault must operate the fastest, and at the same time all of the back-up breakers must remain closed long enough to allow the nearest breaker to open completely and clear the fault.

In all of these calculations the breakers which are farthest away from the source are the first to be set, and these are the 208/120-volt and the 480-volt circuit breakers.

The 480-volt motors should be protected by breakers having thermal magnetic tripping devices, because these devices can be closely coordinated with the motor heating characteristics. Figure 3 shows the time current characteristics of a thermal magnetic trip device on a 25,000-ampere low voltage breaker.

These breakers should have an overload maximum trip setting of approximately 125 percent of motor full-load current for a motor with a temperature rating of 40 C rise, or 115 percent of motor full-load current for any other type motor. In this example it is assumed that there are no motor starters involved, so the breakers in the motor circuit must provide both short circuit and normal overload protection. It is also assumed that the motors have a 40 C temperature rise. Since the 200-horsepower motor has a full-load current of 240 amperes, 125 percent of this current is 300 amperes. Hence the calibration setting of the 300-ampere breaker should be obtained from the 100 percent curve shown in Figure 3.

Calibrations plotted

To determine the setting of the 1000-kva transformer secondary breaker, the time current curve for the largest feeder breaker is plotted on graph paper, shown as curve 1 in Figure 6. The points for this curve may be taken from Figure 3. On this same graph (Figure 6) the time current characteristic of the transformer secondary breaker is also plotted.

The time current characteristic for this 50,000-ampere breaker tripping device is shown in Figure 4. A curve is selected from Figure 4 so that when it is plotted on the graph paper (Figure 6) its time values will exceed the time values for the feeder breaker curve by at least 25 percent over the entire range.

The characteristics of the tripping devices and trial plottings on the graph show that a 100 percent inverse-time load setting, *ITL*, with a 200 percent calibration setting on the main 480-volt breaker will be required. This is then plotted as curve 2 in Figure 6.

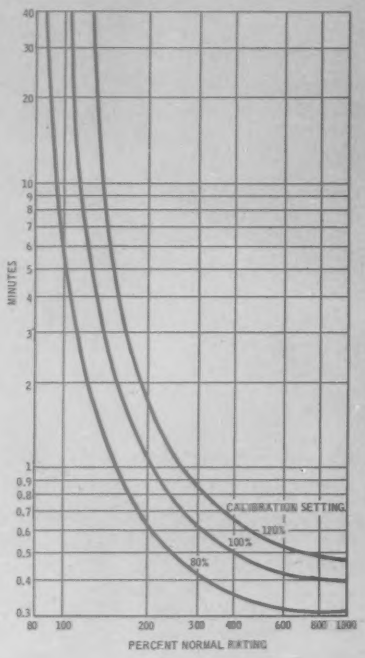


FIGURE 3

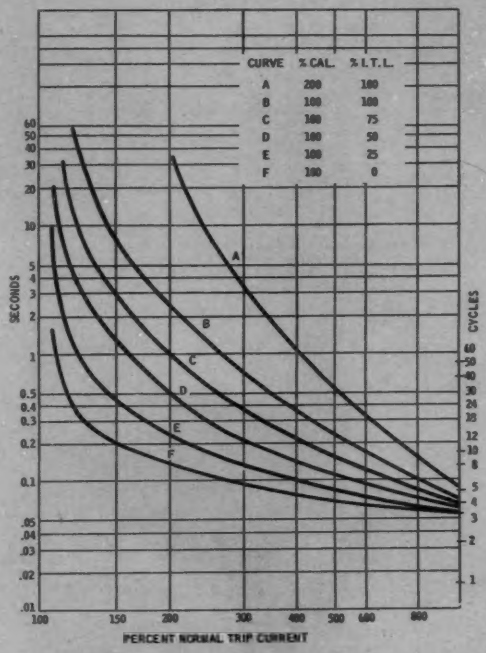


FIGURE 4

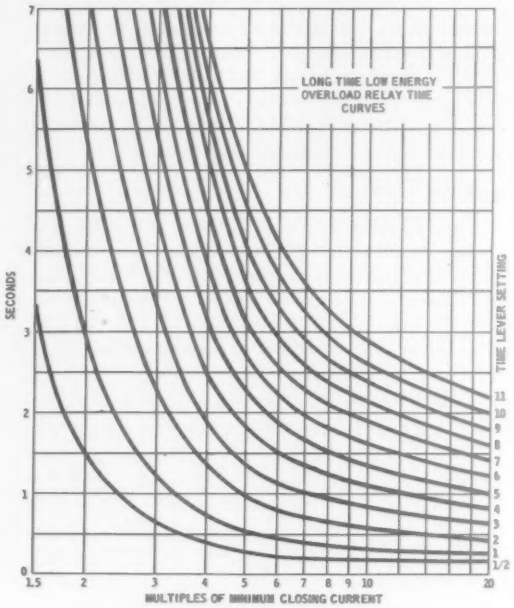


FIGURE 5

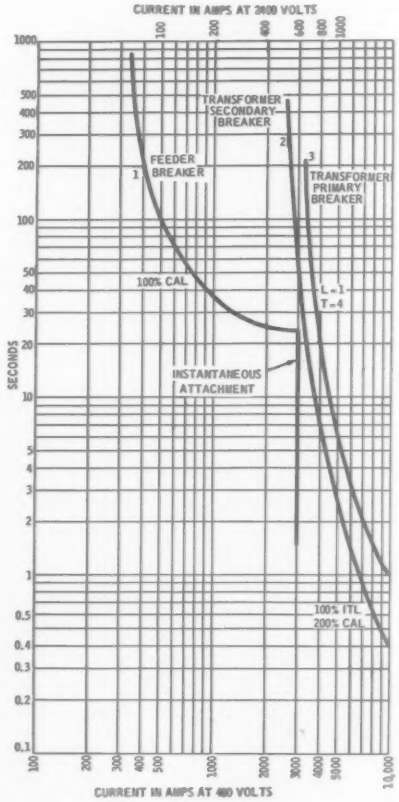


FIGURE 6

The next step in the procedure is to select a setting of the overcurrent relays which operate the 2400-volt breaker on this load center substation. The pick-up of this relay should be selected so that it has a value of approximately 150 percent of the full-load 2400-volt current of this 1000-kva transformer. This full-load current is approximately 240 amperes which will appear to the relay as:

Full load current $\times$ CT ratio = amperes

$$240 \times \frac{5}{500} = 2.4 \text{ amperes}$$

$$2.4 \times 150\% = 3.6 \text{ amps}$$

The four-ampere tap on the overcurrent relay should be chosen because it is the closest setting to the 3.6 amperes. In this particular arrangement, the overcurrent relay has a family of time current characteristic curves as shown in Figure 5. An examination of these curves and trial plottings will show that a time current characteristic corresponding to time lever 1 will coordinate nicely with the 480-volt main breaker time current characteristic. This coordination is shown in Figure 6 as curve 3.

Now, each of these three curves is so coordinated that for a given value of current the device closest to the fault will operate first.

The same procedure is followed for the feeder from the 2400-volt bus to the 208/120-volt bus. On a given feeder fed by a 25,000-ampere breaker, the fuse current characteristic of the lowest fuse or circuit breaker beyond the main circuit breaker is plotted as curve 1 of Figure 7. Figure 8 shows the time current characteristics of the inverse-time series overcurrent device on the G-25 breakers.

Each relay is important

Figure 8 shows that the 100 percent calibration, 50 percent *ITL* characteristic will give a satisfactory coordination with the fuse beyond it. This curve is plotted as curve 2 in Figure 7. The next step is to plot a similar curve for the 150-kva transformer secondary breaker, which in this case is a 600-volt, 25,000-ampere breaker. A curve is selected from Figure 8 that will be slower, at all points, than curve 2, Figure 7. An examination of Figure 8 shows that the curve which represents 100 percent calibration at 25 percent *ITL* coordinates very well with curve 2, Figure 7. This is then plotted as curve 3 in Figure 7. The operating time for the 2400-volt circuit breaker can now be plotted.

As was done for the 1000-kva load center, a pick-up on the overcurrent relay is selected that has a value of approximately 150 percent of the full-load current of the 150-kva transformer. The full-load current in this case is approximately 36 amperes. In terms of the relay current the full-load current will be:

$$36 \times \frac{5}{50} = 3.6 \text{ amperes}$$

and at 150 percent load it will be:

$$3.6 \times 150\% = 5.4 \text{ amperes}$$

Hence a tap setting on the overcurrent relay of 6 amperes was chosen. In this particular case an examination of the relay curves in Figure 5 shows that the minimum lever setting of ½ is more than adequate. This curve when plotted on Figure 7, curve 4, will have all its points at least 200 percent of the minimum time value for the 25,000-ampere transformer secondary breaker.

At this point the two load center feeders are set to operate as they should from the 2400-volt bus to the load. The device closest to the point of fault will operate first, and each device behind it will operate with a safe margin in time to insure selectivity.

Motor starting affects trip settings

The next step, then, is to make a selection of relay settings for the 2400-volt motor feeder circuits. Starting with the 1000-horsepower synchronous motor, a long time or 40-second overcurrent relay for this service is used to protect this motor, since this is considered good practice. The time current characteristics of this relay are shown in Figure 9. A tap setting which will represent approximately 125 percent of the full-load motor current is arbitrarily chosen. Since the full-load current will be approximately 250 amperes, the relay current will be:

$$250 \times \frac{5}{400} = 3.1 \text{ amperes}$$

$$3.1 \times 125\% = 3.9 \text{ amperes}$$

Hence a 4-ampere relay tap should be selected. For the first attempt, a lever setting of 1 is a good practicable minimum setting of this type.

If the normal start of the motor causes the relay to trip the breaker out at this lever setting, increase the lever by the minimum amount, by successive trials, until a lever setting is arrived at that will allow a normal start.

In a similar manner, a setting is selected from Figure 9 for the 500-horsepower induction motor. The full-load current of this motor will be approximately 125 amperes. This will appear to the relay as:

$$125 \times \frac{5}{200} = 3.1 \text{ amperes}$$

$$3.1 \text{ amperes} \times 125\% = 3.9 \text{ amperes}$$

Therefore, the same tap and lever settings apply for this machine as for the 1000-horsepower synchronous motor. Again the lever settings should be checked by actually starting the motor.

The instantaneous attachments on the overcurrent relays for the 2400-volt motor feeder breaker are set at about 20 to 25 percent above locked rotor values. In this case, the locked motor current is assumed to be approximately five times full-load current. For the 1000-horsepower motor 5×250 amperes will be 1250 amperes locked rotor current. 125 percent of this will be 1.25×1250

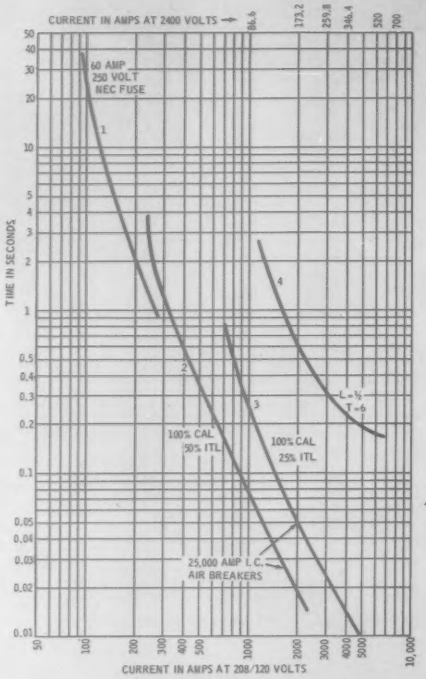


FIGURE 7

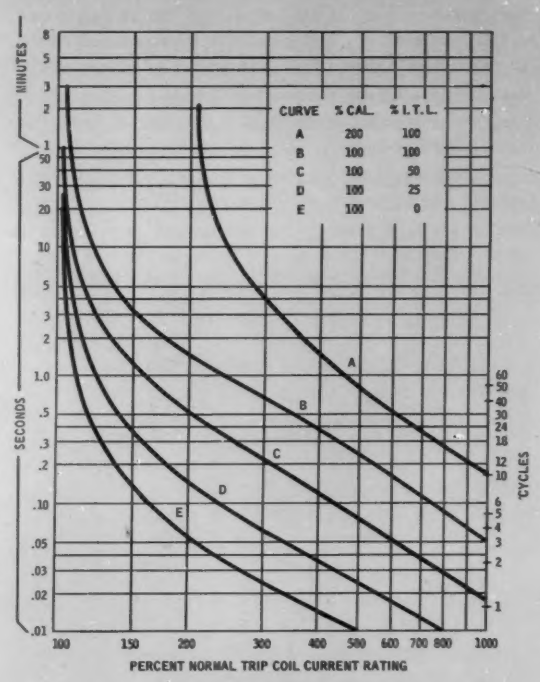


FIGURE 8

or 1562 amperes. Hence the instantaneous attachments will trip at:

$$1562 \times \frac{5}{400} = \text{approximately 20 amperes}$$

In a similar manner it can be shown that the instantaneous attachments for the 500-horsepower motor should also be set at approximately 20 amperes. If actual motor starting shows that an instantaneous attachment trips the breaker, it should be reset higher, by successive trials, until the normal start can be made.

The instantaneous attachment settings are shown in Figure 10 as the vertical portions of curves 1 and 2.

After all of the settings for the 2400-volt feeder breakers have been determined, the next step is to set the relays on the 3000-kva transformer secondary breaker. These settings are found by referring to the 2400-volt feeder breaker tripping time characteristic, curves 1, 2, 3, and 4 in Figure 10. Inspection of these four curves shows that curve 2 for the 1000-horsepower synchronous motor is the slowest.

The setting of 3000-kva power transformer secondary breaker relays at approximately 150 percent of full-load current is the last step in coordinating the various system relay settings. This setting represents a 700×1.5 or 1050 amperes secondary current.

The corresponding relay current is:

$$1050 \times \frac{5}{1000} \text{ or } 5.25 \text{ amperes}$$

A 6-ampere tap setting is chosen from Figure 5 and a lever setting should be selected to give a time current characteristic slower at all points than the slowest curve of a feeder breaker. In this case, the slowest curve would be curve 2 in Figure 10.

Figure 5 shows that lever 1 will give satisfactory coordination. The curve that results from this setting is plotted as curve 5 in Figure 10.

As a final check we must plot on this same graph (Figure 10) the time current characteristics of the 13.2-kv fuse which is protecting the primary of our 3000-kva main transformer. This curve is found in the fuse manufacturer's data and has been plotted as curve 6 in Figure 10.

Examination of Figure 10 shows that all of the protective devices are completely selective. It should be pointed out that in coordinating any circuit breaker with another breaker or with a fuse, at least 0.3 seconds should be allowed between the time current characteristics of successive devices. It is suggested that as the settings are being determined, they should be discussed with the local utility furnishing the power. This will insure that the settings are consistent with the utility's back-up protection.

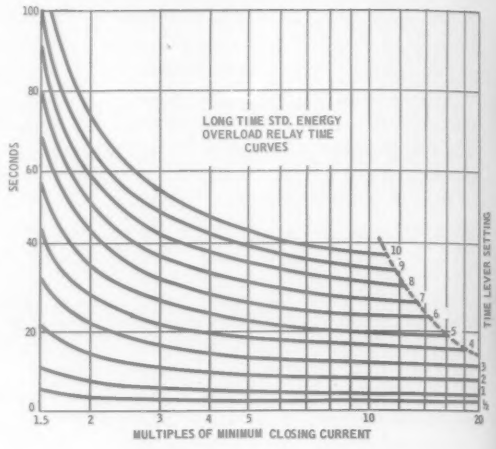


FIGURE 9

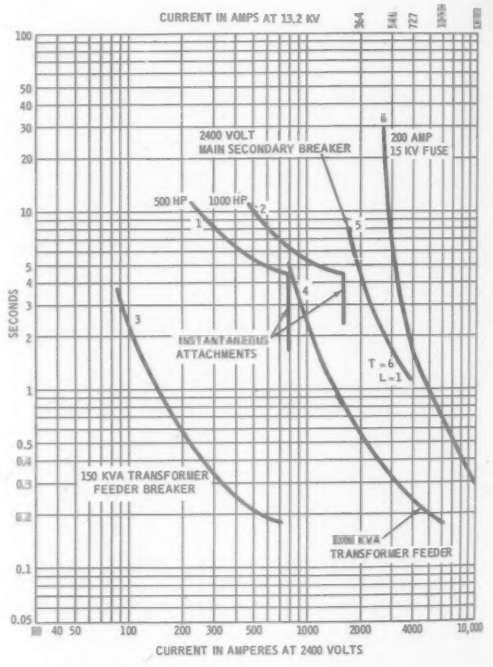
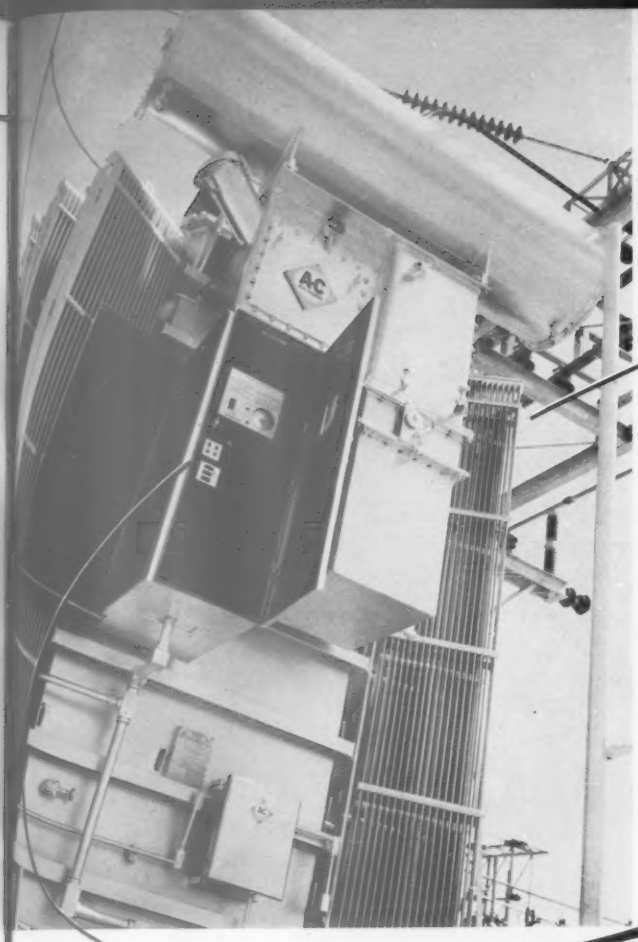


FIGURE 10



CIRCULATING CURRENTS in Paralleled Load Ratio Control Transformers

by **W. C. SEALEY**
Chief Engineer
Transformer Section
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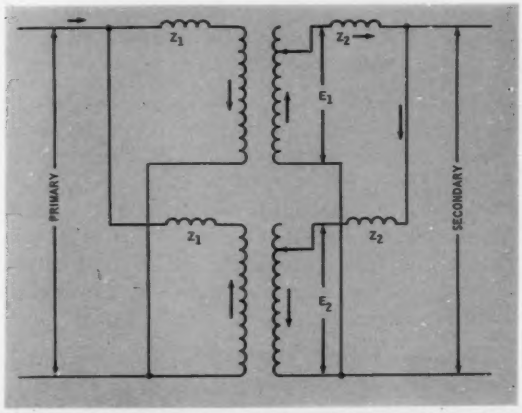
WHEN LOAD RATIO CONTROL TRANSFORMERS are operated in parallel, they must have the same percent impedance if they are to divide the load correctly between them. The other conventional requirement for paralleling is that they have the same ratio of transformation. However, with load ratio control this condition can seldom be achieved under all operating conditions. Since load ratio control mechanisms on paralleled units rarely operate simultaneously, much of the time the ratio of transformation of the two transformers is not exactly the same and circulating current will flow between them. When the value of circulating current is not excessive, satisfactory parallel operation is achieved.

The circuit of Figure 1 shows two load ratio control transformers connected in parallel. The circulating current which flows in the secondary windings of these transformers when their ratios are only slightly different is equal to the difference in no-load voltage of the two transformers when only their primaries are connected in par-



CIRCULATING CURRENTS flow between paralleled transformers when their ratios are even slightly different. **(FIGURE 1)**

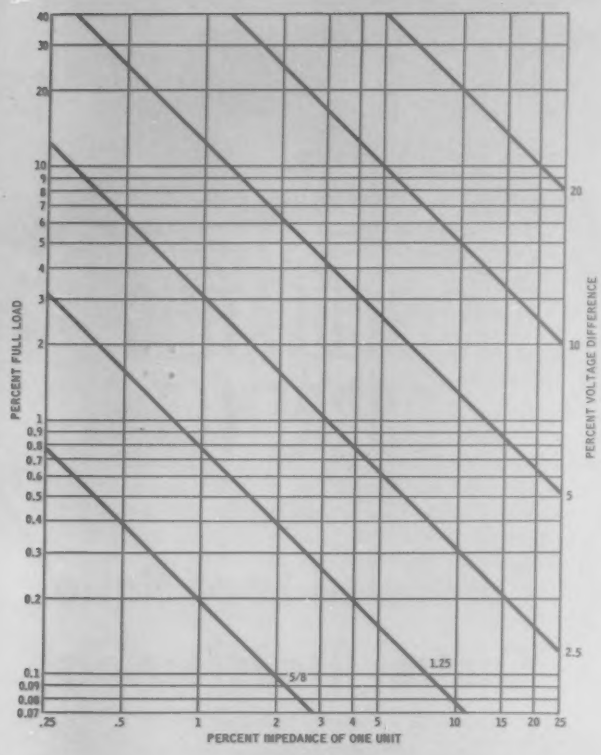


FIGURE 2

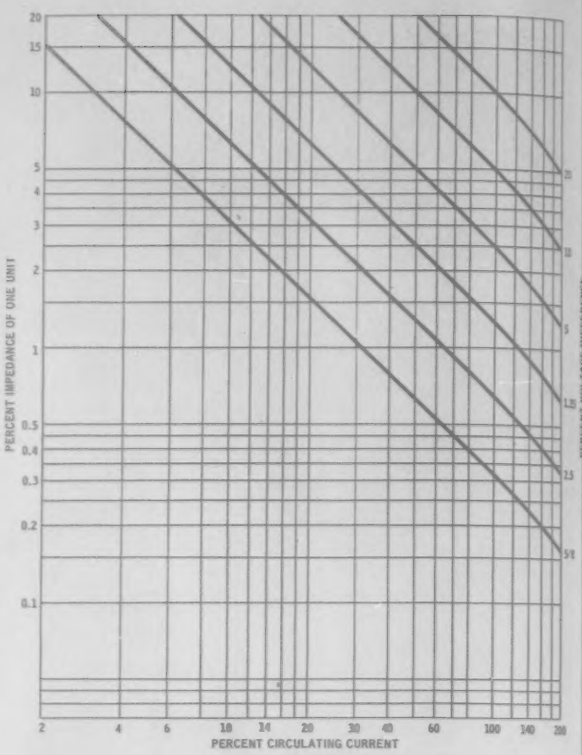


FIGURE 3

allel, divided by the impedance in the path of the circulating current.

The equation is: Circulating current equals $\frac{E1 - E2}{2Z1 + 2Z2}$

where $E1$ = the open circuit secondary voltage of one machine.

$E2$ = the secondary voltage of the other machine.

$Z1$ = the impedance in the secondary line of one machine, including the transformer impedance.

$Z2$ = the impedance in the secondary line of one transformer to the point of paralleling.

Under the best of operating conditions, units may be one step apart either continuously or for short periods of time. The voltage difference causing the circulating current between the two units is equal to the voltage of one step or more. Typical steps used for load ratio control transformers are ⅝, 1¼, 2½, and 5 percent. Under abnormal automatic operation, the voltage difference may be 10 percent for transformers with a 10 percent range in taps, or 20 percent where the tap range is 20 percent.

The curves of Figure 2 show the secondary circulating currents for two transformers connected in parallel. A separate curve is provided for each of the typical values of voltage difference.

Impedance limits circulating currents

Most two-winding transformers have sufficient inherent impedance to limit the circulating current to a reasonable value even for continuous operation when two units are one step apart. For example, two transformers operating one step apart with ⅝ percent steps and 5 percent impedance in each unit will limit the circulating current to 6 percent of full-load current. This condition is shown by the ⅝ percent voltage curve of Figure 2. Even for continuous operation this value of current is not objectionable. If the size of the steps were 2½ percent, the circulating current under this condition would be 25 percent. For continuous operation this current might be satisfactory with a 100 percent power factor load but would probably be unsatisfactory for low power factor loads. However, for momentary service where the control will bring the units to the same step in a short time, this circulating current may not be objectionable even at a low power factor. If the circulating current is at a low power factor and the load current at a high power factor, a circulating current of 25 percent will increase the total value of current in the transformer windings only slightly. If the load current and the circulating current are at right angles to each other, the increase would be only 3 percent.

Autotransformers such as step voltage regulators, on the other hand, have much lower impedances, on some connections being as low as 0.1 percent. With this im-

pedance the circulating current would be in excess of 200 percent of full-load current. This, of course, would be unsatisfactory. In order to provide satisfactory operation with such units, additional impedance must be inserted either in the form of reactors or line impedance. This impedance is placed either in the primary or secondary between the units and the point of paralleling.

Primary line currents are small

The primary line current resulting from circulating currents is the difference between the current circulating in the individual primary windings of the two transformers and is only a fraction of the current flowing in the windings. For example, when 25 percent of full-load current circulates in the transformer windings due to a voltage difference of 2.5 percent and an impedance of 5 percent in each unit, the current flowing in the high voltage line is only .6 percent of full-load current. Figure 3 shows the current taken from the supply line under various conditions of voltage unbalance and paralleling impedance.

When more than two transformers are paralleled, the circulating current is greater than for two. However, with an infinite number of paralleled transformers the circulating current is limited to only twice that of two paralleled transformers.

The circulating current for N transformers equals $\dfrac{E1-E2}{Z1+Z2+\dfrac{Z1+Z2}{N-1}}$ which reduces to the equation:

Circulating current equals $\dfrac{N-1}{N}\times\dfrac{E1-E2}{Z1+Z2}$

The relation to the current of two or more paralleled transformers is shown in Figure 4.

Figure 5 shows the percent impedance required with each unit to limit the circulating current to full-load current for various voltage differences and for various numbers of paralleled units. Figure 6 is a similar set of curves. The impedance corresponding to a circulating current which is either higher or lower than full-load current is inversely proportional to the current value.

Protection is available

Many transformers have a 20 percent range in tap voltages. In order to limit the circulating current to full-load current with this range, each unit would require 10 percent if two units were in parallel, and up to 20 percent with a large number of units in parallel. Under this abnormal condition of operation, the circulating current would generally be excessive for continuous operation and the impedance required, too high.

In order to protect transformers operating in parallel against such excessive circulating currents, auxiliary protection may be provided. Three general types of protective equipment have been used:

(1) Relays which operate when the circulating current between units exceeds a given value.

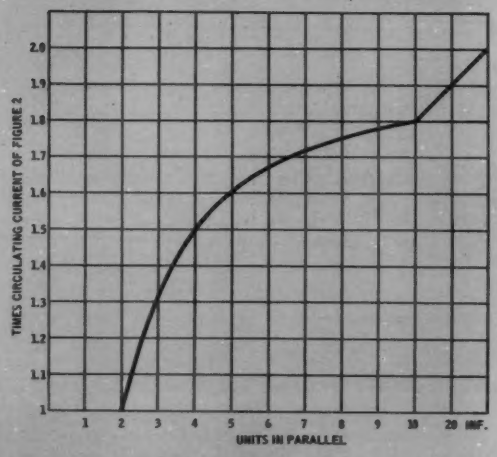


FIGURE 4

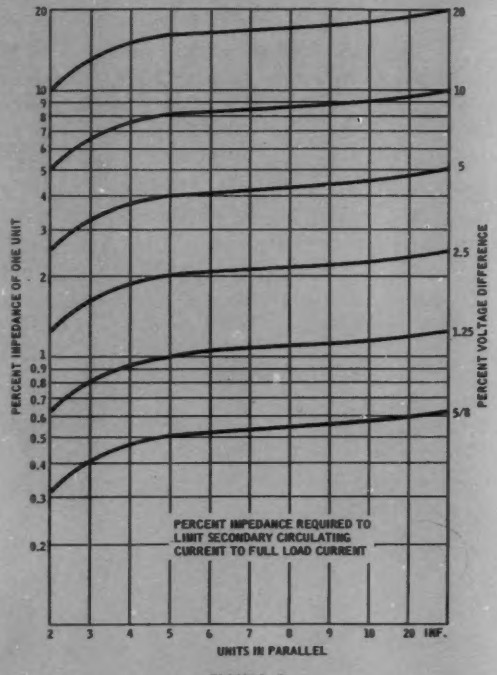

PERCENT IMPEDANCE OF ONE UNIT
PERCENT VOLTAGE DIFFERENCE
PERCENT IMPEDANCE REQUIRED TO LIMIT SECONDARY CIRCULATING CURRENT TO FULL LOAD CURRENT
UNITS IN PARALLEL
INF.

FIGURE 5

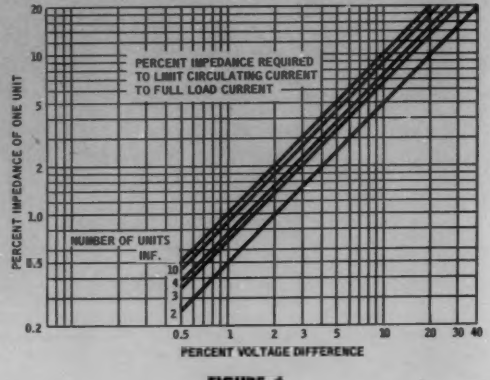

PERCENT IMPEDANCE OF ONE UNIT
PERCENT IMPEDANCE REQUIRED TO LIMIT CIRCULATING CURRENT TO FULL LOAD CURRENT
NUMBER OF UNITS
INF.
PERCENT VOLTAGE DIFFERENCE

FIGURE 6

(2) Relays which operate when the load ratio control devices are too far out of step mechanically with each other.

(3) Thermal relays on the transformer which indicate that the transformer is overheated.

The operation of these relays can either sound an alarm or trip the unit off the line. Relays can also be provided which sound an alarm for a certain amount of current unbalance and trip the units off the line for a still higher amount of unbalance.

The current balance circuit in Figure 7 is an example of the first type of relay equipment. As long as the same current flows in both units, the voltage across the relay *R* will be zero. When the transformers carry different currents, the difference will flow through the relay *R*. When the difference in current reaches a predetermined value the relay *R* will operate.

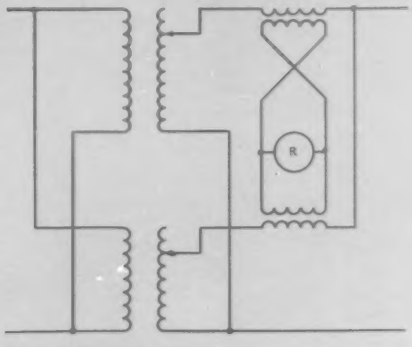
Mechanical position schemes are shown in Figure 8. In Figure 8a a potential transformer is connected across the tapped section of each winding. So long as the mechanisms are on the same physical tap the voltage across the relay *R* will be zero. If the mechanisms are on different taps, the voltage difference appears at the voltage relay *R*. When the difference in voltage becomes sufficiently great, the relay *R* operates.

In Figure 8b the voltage relay *R* is operated by the difference in voltage between the windings *A* and *B*, which may be either potentiometer resistors or tapped autotransformers. In either case the contact on *A* and *B* is mechanically connected to the position indicators of the mechanisms.

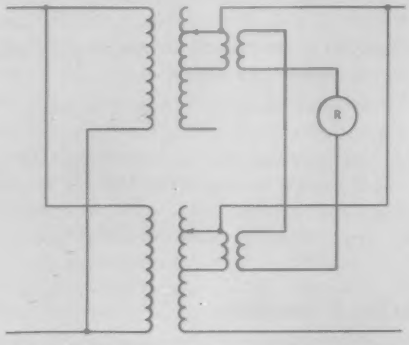
The third scheme utilizes the thermal relays provided for overload protection of transformers. If the load carried by a transformer becomes excessive for a long enough period of time, the thermal relay operates. Most thermal relays utilize a bulb or bimetal element, located in the oil and heated by both the oil and current.

The circulating current between two load ratio control transformers connected in parallel depends upon (1) the number of units in parallel, (2) the impedance of each unit between the points where the unit is connected in parallel with other transformers, and (3) the voltage difference which can exist between the machines. Most two-winding transformers have sufficient inherent impedance for successful parallel operation, but most step regulators and many other autotransformers do not. In such cases additional impedance, either in the form of reactors or line impedance, should be used between the points of parallel connection.

Whenever the maximum circulating current can be excessive, protective relays to sound an alarm or trip the unit out of service should be provided.



CURRENT SENSITIVE RELAY operates when the circulating current becomes excessive. **(FIGURE 7)**



VOLTAGE SENSITIVE RELAY operates on the voltage unbalance between transformers. **(FIGURE 8a)**

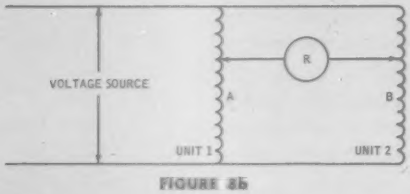
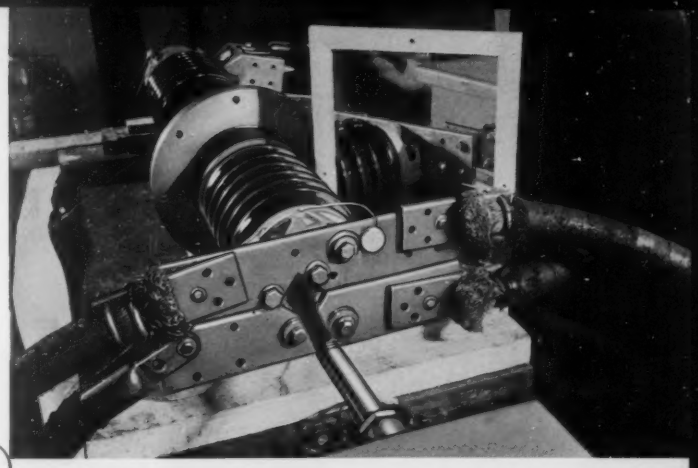


FIGURE 8b

Terminal Bushing Capacity Increased Four Times with...

HYDROGEN JET COOLING



HYDROGEN JET enters the cap of a generator terminal bushing in this test setup and scours heat from inside surfaces before discharging through exhaust holes visible in the mirror.

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AS STEAM TURBINE-GENERATOR RATINGS have increased, various problems have been encountered in the design and ventilation of terminal bushings to keep their temperatures within the allowable rise.

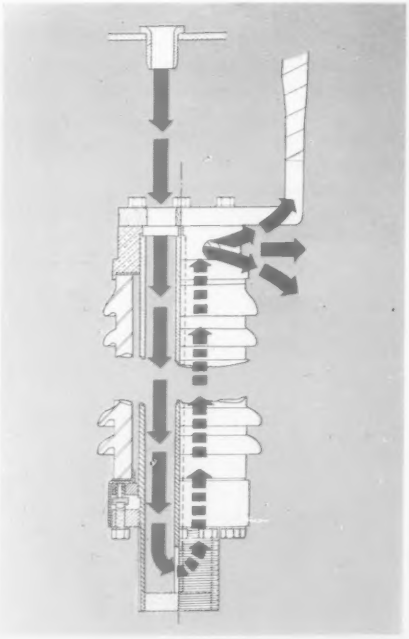
Bushings range up to 6000 amperes capacity, a limitation imposed by the excessive size required for higher ratings. Large size bushings are difficult to handle and install — especially on modern supercharged generators, which are considerably smaller than conventionally cooled machines of the same rating.

In some instances, to reduce the amount of current carried by each bushing, twelve terminals have been used instead of the usual six. However, this presents a serious problem in the mounting of phase-isolating bus ducts.

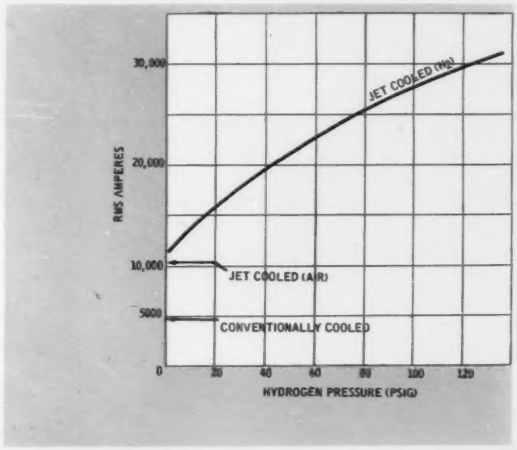
A new method of bushing ventilation has recently been developed which increases the current capacity of a conventional bushing nearly four to one. Heat is removed directly from the area where it is generated by scouring the bushing's hollow copper stud with a jet of cool hydrogen.

A cross section, Figure 1, shows the jet-cooled bushing, with its hollow copper conductor divided by a vertical partition.

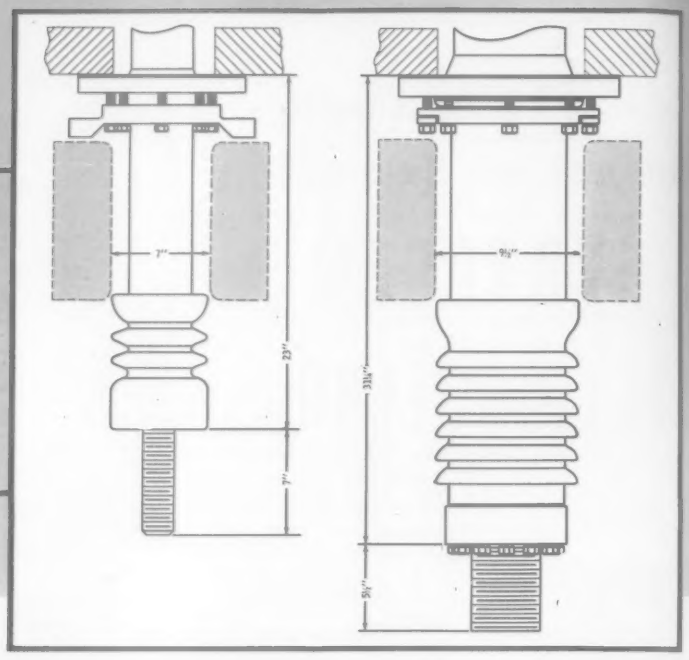
A tube from the fan pressure region of the generator directs a jet of hydrogen to the bushing. Adequate clearance between tube and bushing is maintained by forcing the hydrogen across a six-inch gap before entering the bushing inlet chamber. Gas flows down the complete length of the bushing, on one side of the baffle, and returns along the other side, to discharge through radial holes in the bushing cap into the fan suction region. The entire



JET OF HYDROGEN leaves nozzle from fan pressure region, passes across a six-inch gap, down one side of the internal baffle and up the other side discharging into the generator. **(FIG. 1)**



CAPACITY of a 4500-ampere conventionally cooled terminal bushing is more than doubled with air jet-cooling, increased to 17,500 amperes at 30 psig hydrogen. **(FIGURE 2)**



THE SMALLER BUSHING—jet cooled—has the same rating as the larger conventionally cooled bushing. Projection of porcelain below the current transformer is reduced because the smaller diameter permits a more favorable creepage arrangement below the bottom petticoat. In addition, the weight of porcelain and conductor are both substantially reduced. **(FIGURE 3)**

length of the conductor stud, including the external bus clamping area, is cooled by means of this arrangement.

Jet-cooling takes full advantage of elevated hydrogen pressures. The curve in Figure 2, based on exhaustive tests, shows that even with air, jet-cooling will increase the rating well above that for the same bushing structure when cooled by conventional means. As indicated by the curve, a 4500-ampere bushing safely carries 17,500 amperes with hydrogen pressure at 30 psig. Even at 0.5 psig hydrogen, the bushing carries well over 10,000 amperes. The current capacity of other bushing ratings with jet-cooling will be correspondingly increased.

Jet-cooled bushings will be used to full advantage on supercharged generators. They can be just as effectively used, however, on non-supercharged hydrogen-cooled machines operating at normal pressures, and already are being applied to several large generators of this construction. This development makes standard 13,800-volt machines practical in much larger ratings than ever before.

With jet-cooling, it appears that capacity can be increased to a marked degree, without exceeding safe operating temperatures. At the same time, sturdy mechanical structure is maintained. Generator ratings need no longer be restricted because of limitations imposed by terminal bushings. See Figure 3.

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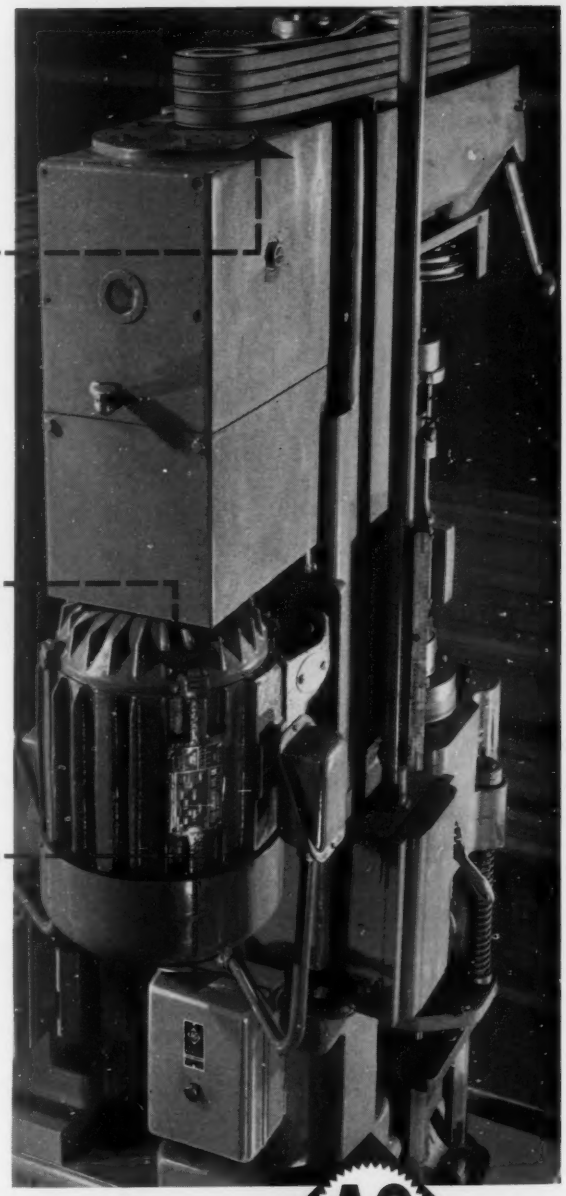

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